

**Lund Institute of Technology
Department of Building Acoustics**

**SOUND INSULATION
IMPROVEMENT
OF FLOATING FLOORS.
A STUDY
OF PARAMETERS.**

Steindor Gudmundsson

**Report TVBA-3017 · ISSN 0281-8477
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PREFACE

The research presented in this report has been carried out at the department of Building Acoustics, Lund Institute of Technology.

The study was initiated by Professor Sven G Lindblad, who has taken a great interest in the work. I am very grateful for his encouragement and guidance.

Mr Robert Månsson has performed the sound insulation measurements, and Mr Lars Persson has given valuable assistance on several occasions. I thank them both for their support.

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Lund, April, 1984

Steindor Gudmundsson

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NOTATION

B	- bending stiffness (Nm^2)
	- bending stiffness per unit width (Nm)
C	- area coupling between two eigenfunctions
D	- longitudinal stiffness modulus (Pa)
E	- Young's modulus (Pa)
E	- Energy (J)
F	- force (N)
	- force per unit width (N/m)
F_N	- source term in modal series
G	- shear modulus (Pa)
G	- parameter of multiple reflections
I	- help parameter for different integral terms
K	- spring stiffness (N/m)
	- spring stiffness per unit area (N/m^3)
L	- length (m)
L_{10}	- normalized impact level (dB)
L_v	- velocity level (dB)
ΔL	- impact sound insulation improvement (dB)
M	- bending moment (Nm)
	- bending moment per unit width (N)
M_0	- single mass (kg)
N	- dynamic stiffness per unit area (N/m^3)
	($N = j\omega Z_p$, $N = j\omega Z_f$)
N	- number, number per unit are etc.
N	- array of numbers
R	- transmission loss (dB)
R_0	- transmission loss for normal incidence (dB)

ΔR	- airborne sound insulation improvement (dB)
S	- area (m^2)
U	- circumference (m)
V	- volume (m^3)
W	- power (watt)
Y	- ratio of resonant vibrations to forced vibrations
Z_M	- mechanical impedance (Ns/m)
Z_P	- plate impedance (Ns/ m^3)
Z_f	- radiation loading (Ns/ m^3)
Z_S	- wave impedance of interlayer (Ns/ m^3)
a	- length (m)
b	- length (m)
c	- velocity of airborne sound (m/s)
c_B	- flexural wave velocity (m/s)
c'_B	- corrected flexural wave velocity (m/s)
c_L	- longitudinal wave velocity (m/s)
c_R	- Rayleigh wave velocity (m/s)
c_S	- velocity of waves in interlayer (m/s)
c_T	- shear wave velocity (m/s)
f	- frequency (Hz)
f_1	- resonance frequency (Hz)
f_C	- critical frequency (Hz)
f_N	- resonance frequency (Hz)
h	- width, thickness (m)
j	- $\sqrt{-1}$
k	- wavenumber (m^{-1})

k_B	- flexural wavenumber (m^{-1})
k_S	- wavenumber of waves in interlayer (m^{-1})
l	- length (m)
m	- mass per unit area (kg/m^2) - mass per length unit (kg/m)
m	- number (0, 1, 2 ...)
n	- number (0, 1, 2 ...)
n	- modal density
p	- pressure (Pa)
r	- radial distance in cylindrical coordinates (m)
r	- coefficient for amplitude and phase of reflected bending wave
r_j	- coefficient for amplitude and phase of reflectional bending wave nearfield
t	- time (s)
v	- velocity (m/s)
w	- angular velocity (rad/s)
w	- equivalent width of coupling strip (m)
x	- integration variable
x, y, z	- cartesian coordinates
Δ	- correction term - difference
Δ_e	- equivalent power bandwidth
Δ_S	- generalized sound insulation improvement (dB)
Γ	- complex reflection factor for sound pressure
α	- absorption coefficient
α	- help parameter ($\alpha = 1 - (1 - \mu) \sin^2 \theta$)

α_{ij}	- four-pole parameter
β	- help parameter ($\beta = 1 + (1 - \mu) \sin^2 \theta$)
β	- angle of incidence in air (rad, deg)
β_s	- angle of incidence in interlayer (rad, deg)
γ	- help parameter ($\gamma = \cos \theta$)
γ	- argument of the reflection factor for sound pressure
δ	- Dirac delta function
δ	- numerical constant ($\delta \ll 1$)
ε	- help parameter ($\varepsilon = \sqrt{1 + \sin^2 \theta}$)
η	- loss factor
η_i	- loss factor
η_{ij}	- coupling loss factor
κ	- wavenumber for coupled bending waves (m^{-1})
λ_B	- flexural wavelength (m)
μ	- Poisson's ratio
ξ	- integration variable
π	- 3.14159...
ρ	- density, density of air (kg/m^3)
ρ_s	- material density of interlayer (kg/m^3)
σ	- radiation efficiency
τ	- transmission coefficient
τ_d	- diffuse transmission coefficient
φ	- argument of the parameter G for multiple reflections
φ	- angle (rad, deg)
ϕ	- angle (rad, deg)
ψ	- eigenfunction
ω	- angular frequency (rad/s)
ω_1	- angular resonance frequency (rad/s)

Some conventions

$\hat{a}$	amplitude
$\check{a}$	Fourier transformed function
a^*	complex conjugate
$\tilde{a}$	RMS-value
$\langle a \rangle$	averaged over area
v_+	wave travelling in the + direction
∇^2	Laplace operator (div grad)

1. INTRODUCTION

1.1 Definition and configuration

The concept "floating floor" in acoustics refers to a certain complementary construction intended to improve the sound insulation qualities of some primary construction, usually a structural floor. This complementary construction consists of a resilient layer supporting the utility floor. The utility floor should not have any other mechanical contact with the rest of the construction.

Some examples on the different usages of floating floors are:

In residential houses to increase the sound insulation between flats.

In different types of technical rooms, isolating the vibrating machinery from the rest of the house.

In ships, mainly to reduce sound radiation from the hull.

The choice of materials and dimensions varies, of course, with the different types of application. In this report we are mainly concerned with the use of floating floors in building acoustics, that is the sound insulation improvement of the structural floor.

In building acoustics the structural floor can usually be regarded as a self-supporting plate. It is often a concrete slab, homogeneous or possibly with different types of cavities or ribs. The concrete may be lightweight or dense, and typical thickness may be 120 - 200 mm.



The floating floor is in some cases relatively heavy, e.g. 30 - 100 mm concrete or asphalt, but it may also be of a lighter type, e.g. a single or a double layer of chip board, or maybe a single layer of plaster board and another of chip board.

These lighter types of floating floors are sometimes used as a complement to lightweight structural floors, such as wood joist floors.

The resilient layer may be one of principally two types: A continuous load-carrying elastic layer, or separate resilient mounts. In the latter case the rest of the airspace is usually filled with a non-load-bearing material with a sufficiently high flow resistance to prevent horizontal propagation in the airspace.

The scope of this report is narrowed additionally as we mainly concentrate on the case of a heavy homogeneous structural slab and a continuous load-carrying elastic layer. The floating floor itself may however be either light or heavy.

The configuration used consequently in this report is shown in the figure below:

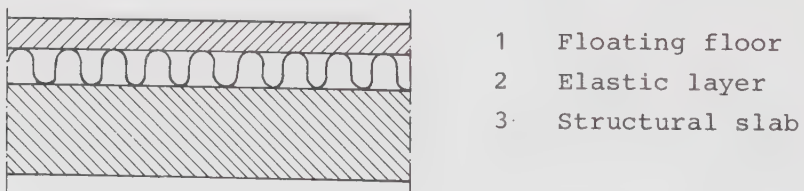


Figure 1.1.1. Configuration.

1.2 Methodics

In the planning of the work it was found convenient to distinguish between different types of theoretical models for floating floors, and to treat each type of model separately.

In chapter 2 we start with locally reacting floating floors, which were first treated by Cremer [1] in 1952, where he deduced his well known and often cited formula

$$\Delta L = 40 \log (f/f_1) \text{ dB} \quad (1)$$

By locally reacting floors it is understood that the impact force on the floating floor is transmitted to the structural slab in the immediate vicinity of the excitation point. The model implies that there is no spatially homogeneous reverberant vibration field in the floating floor. This in turn means that the slab is either infinite or highly damped or so small, that no flexural plate modes exist within the frequency region in question.

In chapter 3, resonantly reacting floating floors are discussed. The most well known work on this type of floors was done by Vêr [2] in 1969, where he treated floating slabs resting on separate resilient mounts. He deduced another, also often cited formula, which in its most simplified form has the frequency dependence:

$$\Delta L \sim 30 \log (f/f_1) \text{ dB} \quad (2)$$

By resonantly reacting floating floors it is understood that the floating slab is thick, rigid and lightly damped, so that the impact force excites a more or less spatially homogeneous reverberant bending wave field. Thus the transmission does not only take

place in the immediate vicinity of the excitation point, but over the whole area.

The terms of locally and resonantly reacting floating floors are due to Vêr [3], [4].

In chapters 2 and 3 the resilient layer is modelled as lumped element, a pure spring with stiffness K (and loss factor η_2). However already at relatively low frequencies, the mass of the materials used as a resilient layer, starts to make itself known. The layer can no longer be treated as a massless spring, but has to be treated as a wave medium with both distributed mass and stiffness.

This problem is treated in chapter 4.

In chapter 5 the measurements in Appendix I - VII are discussed.

The measurements were made in a laboratory, and consist mainly of normalized impact level measurements on different types of floating floors. The airborne transmission loss was also to be measured, but flanking transmission made these measurements difficult. A few such measurements are shown however, in some cases complemented with measurements of acceleration level.

The measured force spectra for the standardized hammer blow on different types of floating floors are also shown.

Finally, in order to evaluate the theoretical results, the material properties of the different elements were measured.

The most interesting properties are the loss factors of the different slabs and of the resilient layer,

together with the dynamic stiffness of that layer. As a complement to the standardized method to measure the dynamic stiffness, an attempt is made to measure instead the wave speed in a medium consisting of the elastic material in question.

In chapter 6 the measurement results are compared to theory and commented on, and finally in chapter 7, some conclusions are drawn.

2. LOCALLY REACTING FLOATING FLOORS

2.1 Historical review

In 1948 H. Cremer and L. Cremer [5] published their "Theory of Impact Noise Excitation", where they consider a homogeneous plate, excited by a point force. They deduced the well known expression for the point impedance of an infinite plate:

$$Z_M = 8\sqrt{mB} \quad (1)$$

However they were shown not to be the first ones to arrive at this expression (see [5], footnote on page 66).

In 1952 L. Cremer [1] published his well known work on the sound insulation of floating floors (later also published in [6] and as excerpt in [7]).

Cremer's model is that of two homogeneous infinite plates with bending stiffness/length and mass/area, coupled with a locally reacting interlayer (springs) with the stiffness

$$K = D_S/h_2 \quad (2)$$

D_S represents the longitudinal stiffness modulus of the material.

The coupled plates are governed by the coupled bending wave equations:

$$\begin{aligned} \nabla^4 v_{Y1} - k_{B1}^4 v_{Y1} &= -\frac{K}{B_1} (v_{Y1} - v_{Y3}) \\ \nabla^4 v_{Y3} - k_{B3}^4 v_{Y3} &= -\frac{K}{B_3} (v_{Y3} - v_{Y1}) \end{aligned} \quad (3)$$

The solution of (3) implies that there occur two pairs of bending-wave fields and nearfields with wavenumbers k_I and k_{II} :

$$k_{I,II}^4 = \frac{1}{2} \left[k_{B_1}^4 \left(1 - \left(\frac{\omega_1}{\omega} \right)^2 \right) + k_{B_3}^4 \left(1 - \left(\frac{\omega_3}{\omega} \right)^2 \right) \right] \\ \pm \sqrt{\frac{1}{4} \left[k_{B_1}^4 \left(1 - \left(\frac{\omega_1}{\omega} \right)^2 \right) - k_{B_3}^4 \left(1 - \left(\frac{\omega_3}{\omega} \right)^2 \right) \right]^2 + k_{B_1}^4 k_{B_3}^4 \frac{\omega_1^2 \omega_3^2}{\omega^4}} \quad (4)$$

The frequencies ω_1 , ω_3 are the resonance frequencies for a simple spring - mass system:

$$\omega_1^2 = K/m_1 \quad \omega_3^2 = K/m_3 \quad (5)$$

The system has a resonance frequency:

$$\omega_{13} = \sqrt{\omega_1^2 + \omega_3^2} \quad (6)$$

Above this frequency k_I , k_{II} tend asymptotically towards k_{B_1} , k_{B_3} respectively. Below ω_{13} k_{II} does not exist, and k_I has the asymptote:

$$k_I^4 = \omega^2 \left(\frac{m_1}{B_1} + \frac{m_2}{B_2} \right) \quad (7)$$

Cremer assumes a point force excitation of the floating slab, producing cylindrically spreading waves with corresponding nearfields. In [5] it is shown that the Hankel function of the second kind is a suitable solution for a single plate. For the coupled-plate problem, two such functions are obtained with arguments $k_I r$ and $k_{II} r$. The boundary conditions at the origin demand that the force components add up to the external excitation force in the floating slab, and add up to zero in the structural slab. In the far field we have propagating cylindrical flexural waves with wave-numbers k_I and k_{II} .

If the floating slab now is assumed to be much thinner than the structural slab, so that $k_{B_1}^4 \gg k_{B_3}^4$, Cremer shows that the wavefield of type II in the floating slab can be neglected, and so can the wavefield of type I in the structural slab. Calling the amplitudes of the remaining waves $v_{1,I}$ and $v_{3,II}$

respectively, Cremer now compares $v_{3,II}$ to the amplitude $v_{3,0}$ he would get by directly exciting the structural slab. When only considering frequencies well above ω_{13} (so that $k_I \approx k_{B1}$ and $k_{II} \approx k_{B3}$) he shows that

$$\begin{aligned}\Delta L &= 40 \log (v_{3,0}/v_{3,II}) \\ &= 40 \log (\omega/\omega_1)\end{aligned}\quad (8)$$

This ΔL , "impact noise reduction improvement" was defined by Gösele [8] as the difference between the sound level in the receiving room with and without the floating floor. Here it is sufficient to calculate the difference in the velocity level of the structural slab, as the waves in both cases are of the same kind, and thus have the same radiation conditions.

In deducing (8) the assumption has been made, that the force spectrum acting on the floating slab is equal to that acting on the structural slab. This is true if the impedance of the exciting hammer ($j\omega M_0$) is small compared to the driving point impedance of each plate (Z_M).

If the floating floor is light, ($|Z_{M1}| \ll \omega M_0$) a correction term must be added to (8):

$$\Delta L = 40 \log \left(\frac{\omega}{\omega_1} \right) + 20 \log \left| 1 + \frac{\omega M_0}{Z_{M1}} \right| \quad (9)$$

The frequency dependence of (8) had already been established empirically by Gösele [9]. In [1] Cremer compares equations (8) and (9) with a measurement on a light floating floor, and finds a good agreement between the measured result and (9). Later it became evident however that (8) overestimates the sound insulation improvement in many cases. This is further discussed in chapter 3.1.

In [7] Cremer and Heckl explain more simply the deduction of (8): In the floating floor, a free bending

wave is excited by a point force. It has the bending wave number k_{B_1} and excites the structural slab through the locally reacting interlayer, producing a forced wave there with the equal wavenumber, k_{B_1} . The amplitudes are related by:

$$v_{3, \text{forced}} = v_{1, \text{free}} \frac{K}{B_3 (k_{B_1}^4 - k_{B_3}^4)} \quad (10)$$

If $k_{B_1}^4 \gg k_{B_3}^4$ this reduces to:

$$\frac{v_{3, \text{forced}}}{v_{1, \text{free}}} = \left(\frac{\omega_1}{\omega} \right)^2 \frac{B_1}{B_3} \quad (11)$$

However the forced wave cannot satisfy the boundary conditions for the structural slab at the origin, where the force components must add up to zero. Another wavefield must be introduced, which is the free wave in the structural slab with wavenumber k_{B_3} . The amplitude of this free wave is related to the forced one as

$$\frac{v_{3, \text{free}}}{v_{3, \text{forced}}} = \left(\frac{k_{B_1}}{k_{B_3}} \right)^2 = \frac{f_{C_1}}{f_{C_3}} \quad (12)$$

If we again assume $k_{B_1}^4 \gg k_{B_3}^4$, we can practically neglect the forced waves.

Introducing $v_{3,0}$ as the amplitude, when the force excites the structural slab directly, we have

$$\Delta L \approx 20 \log |v_{3,0}/v_{3, \text{free}}| \quad (13)$$

Now assuming that the impedance of the hammer is small compared to the driving point impedances of the slabs, we have

$$\frac{v_{1, \text{free}}}{v_{3,0}} = \sqrt{\frac{B_3 m_3}{B_1 m_1}} \quad (14)$$

Summing up we get

$$\begin{aligned}
 \frac{v_{3,0}}{v_{3,\text{free}}} &= \frac{v_{3,0}}{v_{1,\text{free}}} \frac{v_{1,\text{free}}}{v_{3,\text{forced}}} \frac{v_{3,\text{forced}}}{v_{3,\text{free}}} \\
 &= \sqrt{\frac{B_1 m_1}{B_3 m_3}} \left(\frac{\omega}{\omega_1}\right)^2 \frac{B_3}{B_1} \left(\frac{k_{B_3}}{k_{B_1}}\right)^2 = \left(\frac{\omega}{\omega_1}\right)^2
 \end{aligned} \tag{15}$$

and

$$\Delta L \approx 40 \log (f/f_1)^2 \tag{16}$$

2.2 Reciprocity

An interesting detail is the performance of the floating floor construction in the opposite direction.

In this case the structural slab is excited by a point force, with and without the floating slab. Cremer has shown in his work cited above (see [6] e.g.) that above ω_{13} the free wave in the floating floor approximately has the same amplitude as it would have in the absence of the supporting floor and the elastic layer. This approximation obviously applies even better here for the structural slab, and we can assume it to have the same amplitude with and without the floating slab.

The free waves in the structural slab excite a forced wave in the floating slab with the wavenumber k_{B_3} , and the amplitudes are related by

$$v_{1, \text{forced}} = v_{3, \text{free}} \frac{K}{B_1 (k_{B_3}^4 - k_{B_1}^4)} \quad (1)$$

This can be written as:

$$\frac{v_{1, \text{forced}}}{v_{3, \text{free}}} = \left(\frac{\omega_1}{\omega} \right)^2 \left(\left(\frac{f_{C_3}}{f_{C_1}} \right)^2 - 1 \right)^{-1} \quad (2)$$

In the same way as before, in order to satisfy the boundary condition for the floating slab at the origin, free bending waves with wavenumber k_{B_1} have to be introduced. In analogy with eq. (2.1.12) we have the amplitude relation:

$$\frac{v_{1, \text{free}}}{v_{1, \text{forced}}} = \left(\frac{k_{B_3}}{k_{B_1}} \right)^2 = \frac{f_{C_3}}{f_{C_1}} \quad (3)$$

Using here the same approximation as before

$(k_{B_1}^4 \gg k_{B_3}^4)$, we can now practically neglect the free waves, and the difference in velocity level can be written as:

$$\Delta L_v \approx 20 \log |v_{3,0}/v_{1,\text{forced}}| \quad (4)$$

Now $v_{1,\text{forced}}$ is the same type of wave as $v_{3,0}$, as they both have the same wavenumber k_{B_3} , and the same spatial pattern. The two must thus also have the same radiation conditions, and we have

$$\Delta L \approx 20 \log |v_{3,0}/v_{1,\text{forced}}| \quad (5)$$

We have already stated that $v_{3,0} = v_{3,\text{free}}$, so ΔL is given by eq. (2):

$$\Delta L \approx 40 \log (f/f_1) + 10 \log [(f_{c_3}/f_{c_1})^2 - 1] \quad (6)$$

Once again using the approximation $k_{B_1}^u \gg k_{B_3}^u$ we have as before

$$\Delta L \approx 40 \log (f/f_1) \quad (7)$$

Thus the result is the same in both directions, as should be expected for reasons of reciprocity. However, it is interesting to note, that the transmission pattern is quite different. In both cases we have a free bending wave in the primarily excited slab. Now if this is the structural slab with its longer free wavelength λ_{B_3} , it excites a forced wave in the floating floor, which also has this long wavelength.

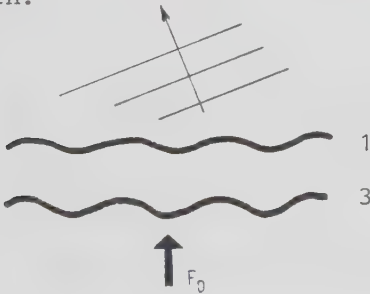


Figure 2.2.1. Schematic excitation of the structural slab and radiation from the floating slab.

On the other hand if the floating slab is the primarily excited one, it has the relatively shorter free wavelength λ_{B_1} , but the excited wave in the structural

slab is the free wave with the relatively long wavelength λ_{B_3} .

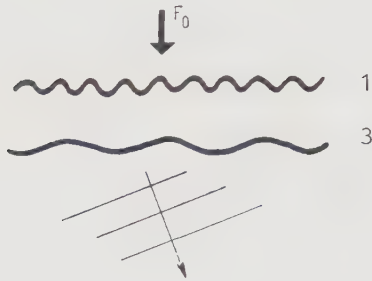


Figure 2.2.2. Schematic excitation of the floating slab and radiation from the structural slab.

Exciting the structural slab with a point force may at first seem abstract and impractical. However there are situations where this model is the more natural one. In [10] Nilsson discusses floating floor constructions for use in ships, and in his model it is the structural slab that is excited, not by a point force however, but by a bending moment. This detail should not make any difference for the calculations, and Nilsson also arrives at the expression (2) for what he calls "the forced solution".

It is natural to call the solution for locally reacting floating floors a forced transmission, when the excitation force is applied to the structural slab (see fig 2.2.1). For many situations it is easier to model the transmission in this "upside-down" way, and this is sometimes practiced later in the report. The result should be indifferent to this choice of transmission direction.

2.3 The Fourier transform method

2.3.1 General solution

In several of his publications (e.g. [6], [11], [12]) Heckl uses the technique of spatial Fourier transforms, when treating point-excited plates. This method is shown to be simpler in problems of sound radiation, than the method of describing the velocity distribution by the sum of two Hankel functions.

In the following we apply the Fourier transform method to the problem of locally reacting floating floors. This method, when applied on point force excitation, should principally give the same result as Cremer's method of Hankel functions cited in 2.1. However we can here in a relatively simple way introduce losses in both the slabs and the elastic layer, and also add radiation loading.

Our general solution does not a priori assume a special type of excitation, so in principle the problem can be solved for different types of excitation.

We start by following Heckl's solution for a single infinite plate in [6].

The spatial excitation distribution is denoted by

$$\hat{p}(x, z) = \frac{1}{4\pi^2} \iint_{-\infty}^{+\infty} \hat{p}(k_x, k_z) e^{jk_x x} e^{jk_z z} dk_x dk_z \quad (1)$$

In a similar way the velocity distribution of the plate is given by

$$\hat{v}(x, z) = \frac{1}{4\pi^2} \iint_{-\infty}^{+\infty} \hat{v}(k_x, k_z) e^{jk_x x} e^{jk_z z} dk_x dk_z \quad (2)$$

The inverse transforms are:

$$\check{p}(k_x, k_z) = \iint_{-\infty}^{\infty} \hat{p}(x, z) e^{-jk_x x} e^{-jk_z z} dx dz \quad (3)$$

and

$$\check{v}(k_x, k_z) = \iint_{-\infty}^{\infty} \hat{v}(x, z) e^{-jk_x x} e^{-jk_z z} dx dz \quad (4)$$

Heckl shows that the radiated power from the infinite plate can be written as:

$$W = \frac{1}{2} \operatorname{Re} \left\{ \frac{1}{4\pi^2} \iint_{-\infty}^{\infty} \rho c \sigma \check{v}(k_x, k_z) \check{v}^*(k_x, k_z) dk_x dk_z \right\} \quad (5)$$

where

$$\sigma = \frac{k}{\sqrt{k^2 - (k_x^2 + k_z^2)}} \quad (6)$$

Thus equation (5) can be written as:

$$W = \frac{\rho c k}{8\pi^2} \iint \frac{+k}{-k} \frac{|\check{v}(k_x, k_z)|^2}{\sqrt{k^2 - k_x^2 - k_z^2}} dk_x dk_z \quad (7)$$

Now the inhomogeneous bending wave equation for a single plate:

$$\nabla^4 v - k_B^4 v = \frac{j\omega}{B} p \quad (8)$$

gives the relation between the excitation $\check{p}(k_x, k_z)$ and the plate response $\check{v}(k_x, k_z)$:

$$\check{v}(k_x, k_z) = \frac{\check{p}(k_x, k_z)}{Z_p} \quad (9)$$

where

$$Z_p = \frac{B}{j\omega} ((k_x^2 + k_z^2)^2 - k_B^4) \quad (10)$$

Inserted into (8) this gives:

$$W_I = \frac{\rho c k}{8\pi^2} \iint \frac{+k}{-k} \frac{|\check{p}(k_x, k_z)|^2 dk_x dk_z}{|Z_p|^2 \sqrt{k^2 - k_x^2 - k_z^2}} \quad (11)$$

The expression (11) can be evaluated for any given excitation function $\hat{p}(x, z)$ by first evaluating the Fourier transform (3) and substituting it into (11).

Now consider the coupled plates in a floating floor construction. Instead of (8) we have here the coupled bending wave equations:

$$\begin{aligned} \nabla^4 v_1 - k_{B_1}^4 v_1 + \frac{K}{B_1} (v_1 - v_3) &= \frac{j\omega}{B_1} p \\ \nabla^4 v_3 - k_{B_3}^4 v_3 + \frac{K}{B_3} (v_3 - v_1) &= 0 \end{aligned} \quad (12)$$

Solving for v_3 we get the equivalence of equation (9):

$$\hat{v}_3(k_x, k_z) = \frac{\hat{p}(k_x, k_z)}{\bar{z}_{p_1} + \bar{z}_{p_3} + \bar{z}_{p_1} \bar{z}_{p_3} / (K/j\omega)} \quad (13)$$

The radiated power now becomes:

$$W_{II} = \frac{\rho c k}{8\pi^2} \int_{-k}^{+k} \frac{|\hat{p}(k_x, k_z)|^2 dk_x dk_z}{|\bar{z}_{p_1} + \bar{z}_{p_3} + \frac{\bar{z}_{p_1} \bar{z}_{p_3}}{K/j\omega}|^2 \sqrt{k^2 - k_x^2 - k_z^2}} \quad (14)$$

We can now define a generalized "sound insulation improvement" for a locally reacting floating floor as:

$$\Delta_S = 10 \log (W_I / W_{II}) \quad (15)$$

where W_I and W_{II} are given by equations (11) and (14) respectively.

2.3.2 Excitation by a point force

Now let us consider the special case of excitation by a point force.

The spatial pressure produced by a point force can be expressed as

$$\hat{p}(x, z) = \hat{F} \delta(x) \delta(z) \quad (16)$$

and from equation (3) we find

$$\hat{p}(k_x, k_z) = \hat{F} \quad (17)$$

Substituting this into equation (11) and changing the variables (setting $k_x = k_r \sin \varphi$ and $k_z = k_r \cos \varphi$) we get:

$$W_I = \frac{\rho c k \hat{F}_I^2}{4\pi} \int_0^k \frac{k_r dk_r}{|Z_{p_3}|^2 \sqrt{k^2 - k_r^2}} \quad (18)$$

where Z_{p_3} now is written as:

$$Z_{p_3} = \frac{B_3}{j\omega} (k_r^4 - k_{B_3}^4) \quad (19)$$

To this we can add the radiation loading:

$$Z_f = \rho c / \sqrt{1 - (k_r/k)^2} \quad (20)$$

which here appears for both sides of the plate, and we can also introduce losses in the plate by complex notation of the bending stiffness:

$$B_3 \rightarrow B_3 (1 + j\eta_3) \quad (21)$$

This now gives

$$\begin{aligned} Z_{3,tot} &= Z_{p_3} + 2Z_f \\ &= j \left[\omega m_3 - \frac{B_3}{\omega} k_r^4 \right] + \left[\frac{2\rho c}{\sqrt{1 - (k_r/k)^2}} + \frac{B_3 k_r^4 \eta_3}{\omega} \right] \end{aligned} \quad (22)$$

If the integration variables are changed once again in equation (18), setting $\xi = k_r/k$, we now get:

$$W_I = \frac{\rho c \omega^2 \hat{F}_I^2}{4\pi} k^2 \int_0^1 \frac{\xi d\xi}{|N_3 + N_0|^2 \sqrt{1 - \xi^2}} \quad (23)$$

with

$$N_3 = [B_3 (k\xi)^4 - \omega^2 m_3] + j \left[\frac{\omega \rho c}{\sqrt{1 - \xi^2}} + B_3 \eta_3 (k\xi)^4 \right] \quad (24)$$

and

$$N_0 = j \omega \rho c / \sqrt{1 - \xi^2} \quad (25)$$

The same procedure on equation (15) for the coupled plates gives:

$$W_{II} = \frac{\rho c \omega^2 \hat{F}_{II}^2}{4\pi} k^2 \int_0^1 \frac{\xi d\xi}{|N_1 + N_3 + N_1 N_3 / N_2|^2 \sqrt{1 - \xi^2}} \quad (26)$$

with N_1 defined analogously to N_3 and

$$N_2 = K(1 + j\eta_2) \quad (27)$$

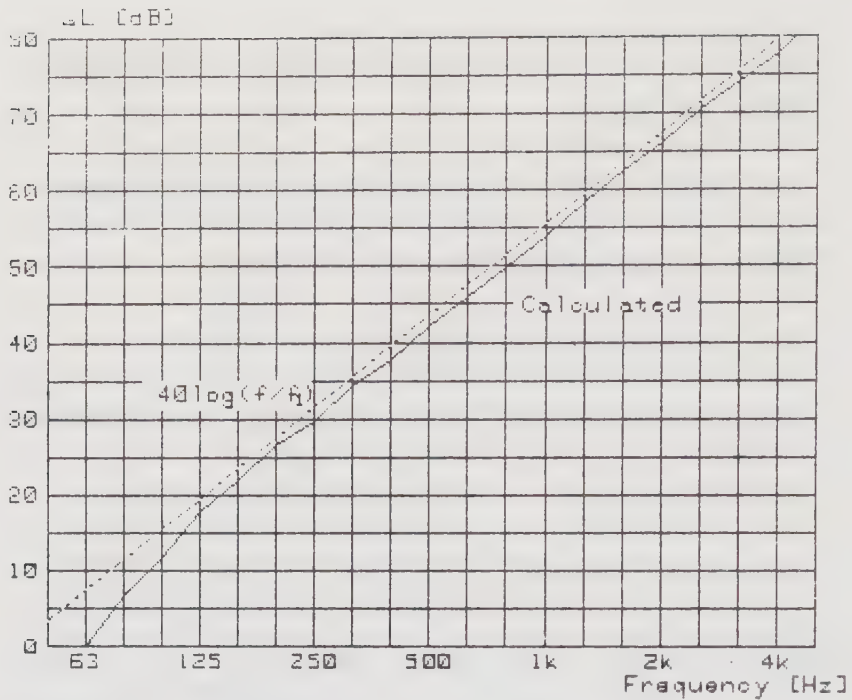
Now if we assume that we have the same force acting on the floating slab as we have on the structural slab by itself ($F_I = F_{II}$), we can write

$$\Delta_S = \Delta L = 10 \log \frac{\int_0^1 \frac{\xi d\xi}{|N_3 + N_0|^2 \sqrt{1 - \xi^2}}}{\int_0^1 \frac{\xi d\xi}{|N_1 + N_3 + N_1 N_3 / N_2|^2 \sqrt{1 - \xi^2}}} \quad (28)$$

This expression is easily solved by numerical integration and several examples are shown below. We have a numerical problem with $\xi = 1$, and this is solved simply by only integrating up to $\xi = 0.99$.

The first example is a 160 mm structural slab and a 50 mm floating slab, both made of dense concrete. The interlayer is 50 mm of mineral wool with a modulus of elasticity 4 times larger than the isothermal modulus of air. This corresponds roughly to 160 kg/m³ rockwool.

The loss factors are chosen as frequency independent, and calculations with several combinations of values have been made. The effect of different loss factors is however only marginal here, and only one combination of these is shown: $\eta = 1\%$ in the floating slab, $\eta = 5\%$ in the structural slab and $\eta = 20\%$ in the mineral wool:



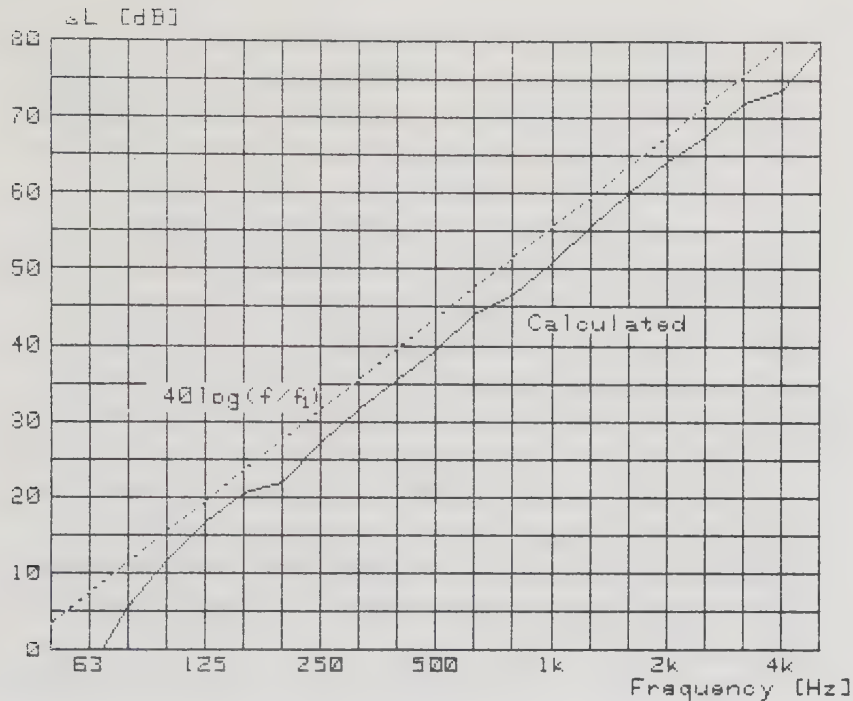
IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200		0.4
	0.160	0.050	2400	30000

Figure 2.3.2.1. ΔL calculated from equation (28).

The agreement with Cremer's solution $40 \log(f/f_1)$ is excellent for frequencies well above f_1 .

Now let us consider another example, where the slabs are a little bit more similar. We choose 200 mm lightweight concrete as a structural slab ($f_c \approx 180$ Hz), but otherwise the same configuration as before. The result is shown below:



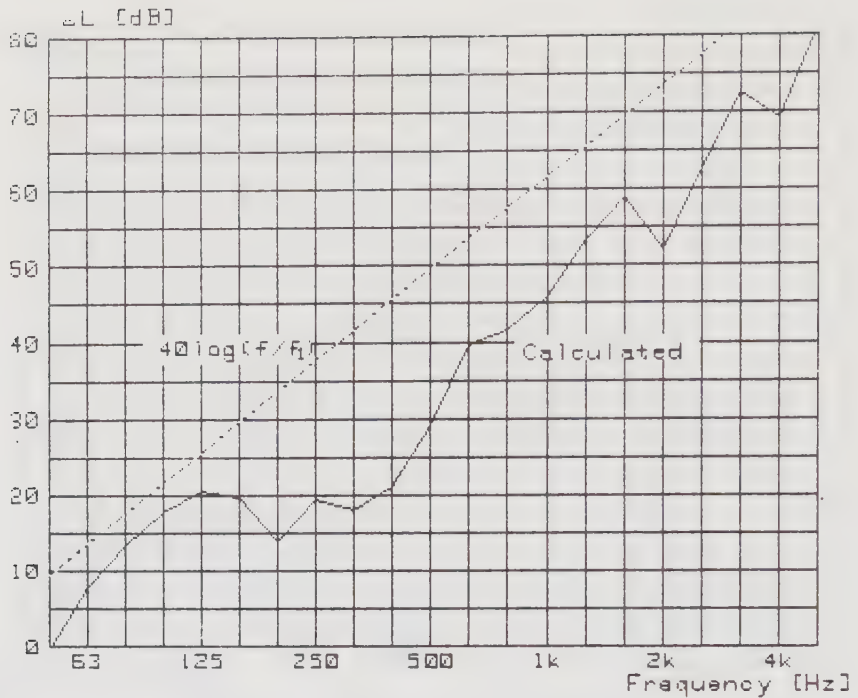
IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200		0.4
	0.200	0.040	850	2250

Figure 2.3.2.2. AL calculated from equation (28).

Here the curve lies a few dB below $40 \log(f/f_1)$, but if we also consider the last term in equation (2.2.6), the agreement with Cremer's solution becomes better. This is true both here and also in figure 2.3.2.1. This correction term is $10 \log((f_{c_3}/f_{c_1})^2 - 1)^2$, and it amounts to -2.5 dB here and -1.0 dB in our first example.

What happens if the two slabs are identical? Cremer's solution is not applicable in this case, but we have nevertheless drawn the curve $10 \log(f/f_1)$ for comparison in our following example of two 100 mm concrete slabs:



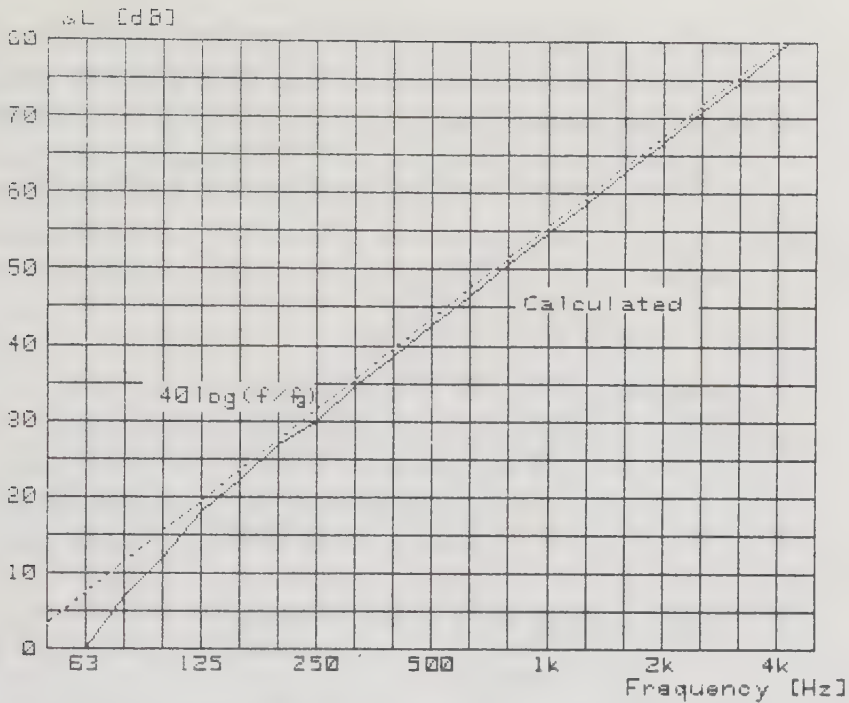
IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.100	0.010	2400	30000
	0.050	0.200		0.4
	0.100	0.050	2400	30000

Figure 2.3.2.3. ΔL calculated from equation (28).

As could be expected, the deviations from the simple equation $40 \log(f/f_1)$ are considerable here.

Our final example is made to investigate whether the reciprocity is true for equation (28). In the calculations we place the structural slab on top, and keep our source fixed. In practice we may consider keeping the configuration fixed and letting the source excite the structural slab instead. The result is shown in figure 2.3.2.4 and it is practically identical to the one in figure 2.3.2.1.



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

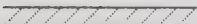
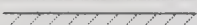
	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.180	0.050	2400	30000
	0.050	0.200		0.4
	0.050	0.010	2400	30000

Figure 2.3.2.4. The case from figure 2.3.2.1 upside down.

2.3.3 Excitation by airborne sound

It has previously been stated that the equations (11) and (14) can be evaluated for any given excitation. However, when the plates are considered to be infinite, and excited by airborne sound over the whole area, the radiated power is infinite, and these equations are not meaningful. However equation (15) for the insulation improvement may still be valid. We have to modify our above statement and say that equations (11) and (14) can be evaluated for any given excitation acting on a finite area of the plates.

The problem of excitation by an incident airborne wave over an infinite plate is the same problem as in the time domain for a pure tone with no start or stop.

Let us first assume that we have normal incidence. Then

$$\hat{p}(x, z) = \hat{p}_0 \quad (29)$$

and from equation (3) we find:

$$\hat{p}(k_x, k_z) = 4\pi^2 \hat{p}_0 \delta(k_x) \delta(k_z) \quad (30)$$

Substituting this into equation (15) we find that

$$\Delta_S = \Delta R_0 = 20 \log \left| \frac{-j\omega m_3 - j\omega m_1 - j\omega^3 m_1 m_3 / K}{-j\omega m_3} \right| \quad (31)$$

or

$$\begin{aligned} \Delta R_0 &= 20 \log (1 + m_1/m_3 + \omega^2 m_1/K) \\ &\approx 40 \log (\omega/\omega_1) \end{aligned} \quad (32)$$

(This simple case is of course more easily analysed considering a system of two lumped masses, connected by a spring, compared to the system of one mass, with both systems excited by the same force.)

If, more generally, we have a plane propagating incident wave

$$\hat{p}(x, z) = \hat{p}_0 e^{jk'_x x} e^{jk'_z z} \quad (33)$$

then

$$\hat{p}(k_x, k_z) = 4\pi^2 \hat{p}_0 \delta(k'_x - k_x) \delta(k'_z - k_z) \quad (34)$$

We now get ΔR for this particular direction of incidence:

$$\Delta R(k'_x, k'_z) = 20 \log \left| 1 + \frac{Z_{p_1}}{Z_{p_3}} + \frac{j\omega Z_{p_1}}{K} \right|^2 \quad (35)$$

where

$$Z_p = \frac{B}{j\omega} [(k'^2_x + k'^2_z)^2 - k^4_B] \quad (36)$$

Let us finally consider excitation by a diffuse sound field. We do this by letting one "representative wave component" vary over all directions of incidence and calculating the average result. As a representative component we choose the plane propagating wave of equation (33) with the Fourier-transform (34).

This leads to an expression that seems fairly equal to the one for the point force case. In fact, the expressions would be identical, if we did not have the solid angle dependence of the incident diffuse sound field to take into account.

We may write the expression for the point force case in a simplified form as:

$$W_0 = \int_0^1 G_0(\xi) \xi d\xi \quad (36a)$$

where $\xi = k_r/k$ (compare to equation (23) for example.)

Now for each diffuse field component we have a wave-number along the surface $k_r = k \cdot \sin \phi$, where ϕ is the angle of incidence and we get a variable $\xi = k_r/k = \sin \phi$ varying from 0 to 1.

For our diffuse field we may write a similar simplified expression as (36a):

$$W_1 = \int_0^{\pi/2} G_1(\phi) \sin \phi d\phi \quad (36b)$$

where $\sin \phi$ represents the solid angle dependence on ϕ and $G_1(\phi) = G_0(\sin \phi)$.

If we change the variable in (36b) into ξ (considering $d\phi = d\xi/\cos \phi$, and $\cos \phi = \sqrt{1 - \xi^2}$) this gives

$$W_1 = \int_0^1 G_0(\xi) \frac{\xi}{\sqrt{1 - \xi^2}} d\xi \quad (36c)$$

This means that we can get an equation for the diffuse sound field by introducing the factor $1/\sqrt{1 - \xi^2}$ into the integrals of equation (28) which becomes:

$$\Delta_S = \Delta_R = 10 \log \frac{\int_0^1 \frac{\xi d\xi}{|N_3 + N_0|^2 (1 - \xi^2)}}{\int_0^1 \frac{\xi d\xi}{|N_1 + N_3 + N_1 N_3 / N_2|^2 (1 - \xi^2)}} \quad (37)$$

However, for the sake of completeness we also give the deduction of (37) by using the diffuse transmission coefficient τ_d .

The classical definition for the transmission coefficient through a plate at different angles of incidence (ϕ) is:

$$\tau(\phi) = \frac{1}{\left| 1 + \frac{Z_p \cos \phi}{2\rho c} \right|^2} \quad (38)$$

where Z_p is the impedance of the plate (see eq. 10).

The equally classical definition of the diffuse transmission coefficient is:

$$\tau_d = 2 \int_0^{\pi/2} \tau(\phi) \cos \phi \sin \phi d\phi \quad (39)$$

After substitution of (38) we get:

$$\tau_d = 8(\rho c)^2 \int_0^{\pi/2} \frac{\tan \phi \, d\phi}{\left| \frac{2\rho c}{\cos \phi} + Z_p \right|^2} \quad (40)$$

And after changing the integration variable to $\xi = \sin \phi$ we can write:

$$\tau_{d_I} = 8(\rho c)^2 \omega^2 \int_0^1 \frac{\xi \, d\xi}{|N_0 + N_3|^2 (1 - \xi^2)} \quad (41)$$

Analogously we get $\tau_{d_{II}}$ for the floating floor:

$$\tau_{d_{II}} = 8(\rho c)^2 \omega^2 \int_0^1 \frac{\xi \, d\xi}{|N_1 + N_3 + N_1 N_3 / N_2|^2 (1 - \xi^2)} \quad (42)$$

Then as $R = 10 \log (1/\tau_d)$ we have:

$$\Delta R = 10 \log \left(\frac{\tau_{d_I}}{\tau_{d_{II}}} \right) \quad (43)$$

which is the same as equation (37).

In part 2.3.4 it is shown that equation (37) for ΔR and equation (28) for ΔL give practically the same results.

2.3.4 The relation between transmission loss and impact level

This relation was already treated in 1963 by Heckl and Rathe [13]. (See also Heckl [6] and Vér [4] e.g..)

When the standard ISO tapping machine is used to measure the impact level, and if the force spectrum is assumed to be "white" in the frequency region of interest, the above relationship for a bandwidth of an octave can be written as:

$$L_{10} + R = 43 + 30 \log f \quad (44)$$

Our equations (23) and (41) with the same assumptions give:

$$L_{10} = 10 \log \left[\frac{\rho c \omega^2 \hat{F}^2 k^2 (4/10)}{4\pi \cdot 10^{-12}} I_F \right] \quad (45)$$

$$R = -10 \log [8(\rho c)^2 \omega^2 I_T] \quad (46)$$

where $\hat{F}^2 = 4\sqrt{2} N^2$ and I_F and I_T denote the integrals in the respective equations. When added, equations (45) and (46) give:

$$L_{10} + R = 43 + 30 \log f + 10 \log \left(\frac{I_F}{I_T} \right) \quad (47)$$

In the case of a homogeneous plate we here have a correction term to equation (44):

$$\Delta_I = 10 \log \left(\frac{\int_0^1 \frac{x dx}{|N_0 + N_3|^2 \sqrt{1-x^2}}} {\int_0^1 \frac{x dx}{|N_0 + N_3|^2 (1-x^2)}} \right) \quad (48)$$

In the case of the floating floor construction we have the correction term:

$$\Delta_{II} = 10 \log \left(\frac{\int_0^1 \frac{x dx}{|N_1 + N_3 + N_1 N_3 / N_2|^2 \sqrt{1-x^2}}} {\int_0^1 \frac{x dx}{|N_1 + N_3 + N_1 N_3 / N_2|^2 (1-x^2)}} \right) \quad (49)$$

Numerical calculations show that both Δ_I and Δ_{II} are relatively small, but may however have a numerical value of a few dB. Equation (44) can therefore only be said to be approximate. However it has been shown numerically for several floating floor constructions, that Δ_I and Δ_{II} are almost identical. This means that equation (44) can be used with good precision as a relative measure. Thus we can state that if L_{10} is reduced by ΔL , when the floating floor is introduced, then the transmission loss must increase by the same amount ($\Delta R = \Delta L$).

In [14] Cremer discusses the relation between L_{10} and R for floating floors, referring to the work done by Heckl and Rathe. He concludes that their result (44) must lead to $\Delta R = \Delta L = 40 \log(f/f_1)$, and that this is most easily for the special case of normal incidence.

Now, why is equation (44) not exact? In deriving it, one simplification is made: The expression for the diffuse transmission loss can be written as:

$$R = 10 \log \frac{\langle \tilde{p}^2 \rangle}{4 \langle \tilde{v}^2 \rangle (\rho c)^2 \sigma} \quad (50)$$

where σ in general is different from the radiation efficiency of a panel excited by a point force. However if equation (50) is restricted to frequencies above the critical frequency of the plate, it can be assumed that $\sigma \approx 1$, independent of the type of excitation.

This was done in deriving equation (44), which in complete form can be written as:

$$L_{10} + R = 43 + 30 \log f - 10 \log \sigma \quad (51)$$

$$L_{10} + R = 43 + 30 \log f + 5 \log (f - f_c/f)$$

$$f > f_c \quad (52)$$

or

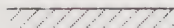
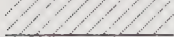
$$L_{10} + R \approx 43 + 30 \log f \quad f \gg f_c \quad (53)$$

Even if the force spectrum is not white as assumed in the above derivations, we still have $\Delta R = \Delta L$ if the force spectrum is the same for both the floating slab and the structural slab. An example would be concrete slabs with the same resilient surface layer.

Below are some numerical examples of Δ_I , Δ_{II} and the term $5 \log (1 - f_c/f)$. We have chosen as examples the first two constructions that we previously used in part 2.3.2. The configurations are repeated at the top of each table.

We see that the three different columns in each table agree reasonably well. However we may note that equation (44) is not exact within 1 dB until at a frequency about two octaves above f_c . Below f_c the correction terms are of the order of magnitude -3 to -4 dB.

Tables 2.3.4.1 - 2. Different correction terms for equation (44).

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200		0.4
	0.160	0.050	2400	30000

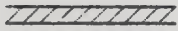
Freq [Hz]	Δ_I [dB]	Δ_{II} [dB]	$5 \log(1-f_c/f)$ [dB]
50	-3.0	-2.9	
63	-3.1	-3.3	
80	-3.4	-3.6	
100	-4.0	-4.2	
125	-4.5	-4.2	-4.5
160	-2.6	-2.6	-2.5
200	-1.8	-1.8	-1.7
250	-1.2	-1.2	-1.2
315	-0.9	-1.0	-0.9
400	-0.7	-1.4	-0.7
500	-0.5	-0.7	-0.5
630	-0.4	-0.6	-0.4
800	-0.3	-0.5	-0.3
1000	-0.2	-0.3	-0.3
1250	-0.2	-0.2	-0.2
1600	-0.1	-0.2	-0.2
2000	-0.1	-0.1	-0.1
2500	-0.1	-0.1	-0.1
3150	-0.1	-0.1	-0.1

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200		0.4
	0.200	0.040	850	2250

Freq [Hz]	Δ_I [dB]	Δ_{II} [dB]	$5 \log(1-f_c/f)$ [dB]
50	-2.8	-2.6	
63	-2.8	-3.0	
80	-2.9	-3.1	
100	-3.0	-3.2	
125	-3.3	-3.5	
160	-3.7	-4.0	
200	-4.8	-4.9	-6.4
250	-3.0	-3.0	-3.1
315	-2.0	-2.3	-2.0
400	-1.4	-2.4	-1.4
500	-1.0	-1.3	-1.0
630	-0.8	-0.9	-0.8
800	-0.6	-0.8	-0.6
1000	-0.5	-0.6	-0.5
1250	-0.4	-0.4	-0.4
1600	-0.3	-0.3	-0.3
2000	-0.2	-0.2	-0.2
2500	-0.2	-0.2	-0.2
3150	-0.1	-0.1	-0.1

In order to point out more clearly that it is the radiation efficiency of the structural slab that counts here, we also calculate for the upside down configuration of table 2.3.4.1. That is to say that here it is the structural slab that is excited and the floating slab that radiates. Still, as the transmission is forced, the floating slab radiates with the radiation efficiency of the structural slab:

Table 2.3.4.3. The case of table 2.3.4.1 upside down.

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.150	0.050	2400	30000
	0.050	0.200		0.4
	0.050	0.010	2400	30000

Freq [Hz]	Δ_I [dB]	Δ_{II} [dB]	$5 \log(1-f_c/f)$ [dB]
50	-3.0	-2.8	
63	-3.1	-3.2	
80	-3.4	-3.5	
100	-4.0	-4.2	
125	-4.5	-4.1	-4.5
160	-2.6	-2.6	-2.5
200	-1.8	-1.8	-1.7
250	-1.2	-1.2	-1.2
315	-.9	-.9	-.9
400	-.7	-.7	-.7
500	-.5	-.5	-.5
630	-.4	-.4	-.4
800	-.3	-.3	-.3
1000	-.2	-.2	-.3
1250	-.2	-.2	-.2
1600	-.1	-.1	-.2
2000	-.1	-.1	-.1
2500	-.1	-.1	-.1
3150	-.1	-.1	-.1

3. RESONANT TRANSMISSION

3.1 Historical review

Again we start in 1952 with Cremer's classical work [1] on the sound insulation of floating floors, where he deduced his well known "surprisingly simple" expression

$$\Delta L = 40 \log (f_1/f) \text{ dB} \quad (1)$$

It soon became apparent, that although the expression (1) in some cases agreed well with measurements, considerable deviations were more often found.

Cremer himself accounts for one such deviation in [1], where (1) underestimates the impact sound insulation improvement of a light floating floor. A correction term has to be added to the expression (1) to account for the relatively small driving point impedance of the light floor (see equation 2.1.9). In most cases however, the expression (1) is found to overestimate the insulation improvement.

In 1954 Cremer [15] publishes his theories on "sound bridges" in floating floor constructions. (Also published later in [6].) Ever since, this has been the most popular explanation for deviations from (1).

Gösele [16] in 1956 points out that the expression (1) shows the best agreements with measurements, when the elastic interlayer is relatively stiff. He suggests two transmission paths besides the forced wave transmission of (1) in an attempt to explain the often encountered overestimated insulation improvement. One of these paths is the propagation of airborne sound in the airspace between the plates. However, Gösele points out that even if such propagation is prevented

by a sufficiently high flow resistance in the inter-layer, there still is considerable disagreement between the expression (1) and his measurements, which were made for a concrete floating floor on top of 10 mm of mineral wool.

The other additional path suggested by Gösele is resonant transmission. To investigate this he made a supplementary measurement, in which the floating slab was larger than the structural slab. Gösele now compared the results for impact excitation above the structural slab to those for excitation outside the structural slab. The excitation above the structural slab was shown to give considerably higher transmission at low frequencies, but at high frequencies the two different sites of excitation gave almost the same result. This suggests for this particular measurement that local transmission dominates at low frequencies, and here the expression (1) should be valid, but at higher frequencies resonant transmission has to be taken into account. Gösele does not estimate the influence of resonant transmission quantitatively.

In 1957 Foti [17] publishes his theory, where he takes into account the finite length of both the structural slab and the floating slab. He describes the vibrations of both slabs by their sums of eigenfunctions. In order to simplify the tedious calculations he assumes the same boundary conditions for both slabs (simply supported). Through this completely different attack Foti still achieved the same frequency dependence as Cremer in (1).

Now Foti's method, using the sums of eigenfunctions, surely must include both local and resonant transmission. His result may therefore at first seem somewhat surprising, considering Gösele's previously mentioned measurement.

Foti's result is discussed by Cremer and Heckl [7] in 1959. They explain, that if the boundary conditions for both slabs are set equal, there will be no resonant coupling between the slabs. As in Cremer's original work they assume a dominating free wave being excited in the floating floor by a point force. This free wave excites a forced wave in the structural slab through the locally reacting interlayer. At the excitation point a free wave must arise in the structural slab to satisfy the boundary conditions. This model gives Cremer's solution for an infinite plate, discussed in part 2.1.

When the slabs are now considered to be finite, there are boundary conditions to be fulfilled along the edges as well. If those boundary conditions are identical for both slabs, they can be satisfied for the structural slab by the forced waves themselves. The solution is thus the same as for infinite plates. However, if the boundary conditions for the two slabs are assumed to be different, as they usually are in practice, then the forced waves in the structural slab cannot by themselves satisfy the boundary conditions. An additional wave field of free waves must arise, with resonant transmission as a result.

Cremer and Heckl set up a one-dimensional equation for the combined wave field in the structural slab (beam), assuming that it is simply supported and has a finite length.

If the floating slab of the same length is also set to be simply supported, then the wave field in the structural slab reduces to that of forced waves as predicted. However, if the floating slab is assumed to have both ends free, there will remain a wave field of free waves, besides the forced waves.

Numerical calculations are only carried out for one particular frequency, for which the free vibrations are shown to be much larger than the forced ones. The conclusion is then drawn, that the free vibrations will also dominate over the forced ones, when averaged over a frequency band.

In their article [7] Cremer and Heckl compare several measurements with the expression (1). They point out that the agreement is good for an asphalt floor which is highly damped, and thus has very little resonant vibrations.

An attempt is made to explain the previously mentioned deviations reported by Gösele [16] with the propagation of sound in the airspace between the slabs. The effect is found to be negligible when mineral wool is used as an elastic interlayer, and although it can explain the low-frequency deviations in the case of air only, it is not sufficient to explain the high-frequency deviation.

The conclusion is that different boundary conditions of the two slabs lead to dominating resonant transmission (except perhaps at low frequencies) for a floating slab with low damping. They state that because of the varying boundary conditions in practice, it "unfortunately seems unlikely" that the effect of resonant transmission can be generalized in a single formula. No further attempt is made to estimate the quantitative influence of the resonant transmission. What remains, they state, is the expression (1) which can be regarded as optimum, and to achieve results close to this optimum they recommend a highly damped floating slab and an elastic layer with high flow resistance, such as mineral wool.

In 1969 Vêr [2] treats resonantly reacting floating floors with "statistical energy analysis" (SEA). He considers however separate load bearing mounts instead of a continuous elastic foundation. He makes several assumptions in deriving his result, some of which are:

- (i) No correlation of slab movements at different mounts.
- (ii) No sound propagation in the airspace parallel to the slabs (the airspace is filled with non-load-bearing mineral wool).
- (iii) The mounts are considered to be pure springs.

Vêr derives a formula, which after some simplifications (high frequency asymptote and same material in both slabs) can be written as:

$$\Delta L = 10 \log \frac{2.3 c_{L1} m_1^2 \eta_1 h_1 \omega^3}{NK^2} \quad (2)$$

(See also [3] and [4].)

In the case of a frequency independent loss factor, ΔL increases here by 30 dB/decade. Another important feature is that ΔL increases with increasing loss factor. In [3] Vêr compares (2) with a measurement by Gösele, and finds good agreement. He also points out that some carefully measured ΔL curves do not follow either the 40 dB/decade slope predicted by (1) nor the 30 dB/decade slope in (2). He suggests that the explanation lies in the frequency dependence of either the loss factor or the dynamic stiffness of the resilient mounts. In [2] Vêr also shows that the improvement for point-force excitation (ΔL) and the improvement in transmission loss (ΔR) are "practically identical" for his case of separate mounts.

Vêr's results are not directly applicable on the case of a continuous resilient interlayer. It may

at first seem tempting just to let the number of mounts in his model increase until the interlayer can be regarded as continuous. Doing this we would however obviously violate his important assumption (i) that the slab movements at different mounts are uncorrelated.

In 1977 Nilsson [10] publishes his studies on the acoustical properties of floating floors. His model is a finite simply supported bottom plate, excited by a bending moment at one of the supports. The top plate is supported by a continuous elastic interlayer and has the same dimensions as the bottom plate, but the edges are assumed to be free.

The calculations are carried out on a one-dimensional beam model, and then the results are converted into a two-dimensional case by replacing the beam length, l , by $2 \cdot S/U$ and l^2 is replaced by S . The elastic interlayer is modelled as locally reacting (springs). Now instead of going through the sums of eigenfunctions of the beams, Nilsson finds the frequency and space average of the velocities by integrating over one resonance top and normalizing with the mode density, and he also makes a normalized integration along the beam-length.

Nilsson's work is more thoroughly studied in part 3.2, but his main results are the following: For the forced transmission Nilsson gets the same solution as we get from Cremer's model, when the structural slab is excited (see part 2.2). Nilsson calculates the difference in velocity level between the two plates, but for the forced transmission this is equal to the insulation improvement as explained in part 2.2:

$$\Delta L_v = \Delta L = 40 \log (f/f_1) + 20 \log |(f_{c_3}/f_{c_1})^2 - 1| \quad (3)$$

For the resonant transmission Nilsson deduces separate expressions for each of the three cases: ($k_{B_1} \gg k_{B_3}$), ($k_{B_1} \ll k_{B_3}$), ($k_{B_1} = k_{B_3}$). The most practical case for building acoustics is the one of a relatively thin floating slab ($k_{B_1} > k_{B_3}$). For this case Nilsson's expression has the form:

$$\Delta L_V = 10 \log \left[\frac{f^{5/2} (f_{C_3}^2 - f_{C_1}^2)^2 4\pi\eta_2 S}{f_1^2 f_{C_3}^3 f_{C_1}^{1/2} \cdot 6 \cdot c \cdot U} \right] \quad (4)$$

[Note that the factor "6" is missing in Nilsson's final expression, equation (37) in [10].]

If η_2 (loss factor for the springs) is independent of the frequency, this implies that ΔL_V increases by 25 dB/decade for the resonant transmission. Here ΔL_V is different from the improvement ΔL as the floating slab and the structural slab have different radiation efficiencies. As discussed in part 3.2, Nilsson has perhaps driven his simplifications one step too far in (4), so that it is usually only applicable in a narrow frequency region in this form.

Nilsson presents measurements, where ΔL_V is shown to agree well with his theoretical results. His constructions are light plates with a thick layer of mineral wool as an interlayer, and ΔL_V varies with frequency by 20 - 25 dB/decade.

Nilsson states that only the stiffness of the mineral wool itself should be considered, and excludes the stiffness of the entrapped air. This is justified, he says, if the plates are sufficiently heavy ($m > 15 \text{ kg/m}^2$) and the mineral wool layer is thick. The difference encountered between different qualities of the mineral wool, Nilsson explains by increasing values of the loss factor with the decreasing density of the mineral wool.

In 1979 Ljunggren [18] publishes his model measurements on floating floors, where the influence of different parameters is studied. He also discusses the work of Cremer and Nilsson and sets up a SEA-model on the basis of the work of Price and Crocker [19], on sound transmission through double panels. Ljunggren assumes excitation of the floating floor and has three sub-systems: The two plates and the interlayer, which he models as an airspace, lined with an absorbing material (absorption coefficient α) round the edges. He neglects the reverse power flows from the airspace to plate 1 and from plate 3 to the airspace, and derives the following equation for the sound insulation improvement ΔL :

$$\begin{aligned}\Delta L &= 10 \log \left[\left(\frac{\omega m_1}{2\rho c} \right)^2 \frac{4c\eta_1\alpha(U/S)}{\pi^2 f_{c_1} \sigma_1 \sigma_3} \right] \\ &= R_0 + 10 \log \left(\frac{4c\eta_1\alpha U}{\pi^2 f_{c_1} \sigma_1 \sigma_3 S} \right)\end{aligned}\quad (5)$$

[Note that Ljunggren erroneously uses h_2 , the thickness of the interlayer, instead of (S/U) .]

If we assume all the factors η_1 , α , σ_1 , σ_3 to be independent of frequency we have here an increase in ΔL by 20 dB/decade. The modelling of the interlayer seems to be rather too abstract, but this is an interesting way of attacking the problem, and further discussions follow in part 4.4.

Ljunggren makes several model measurements on plaster boards with expanded plastic as an interlayer. The results are as follows:

(i) loss factor:

$$\begin{array}{ll} \Delta L \sim 10 \log \eta_1 & f > f_{c1} \\ \text{probably no influence} & f < f_{c1} \end{array}$$

(ii) frequency variation:

in average ΔL seems to vary close to 20 dB/decade

(iii) width of interlayer:

$$\begin{array}{ll} \text{no influence} & f > f_{c1} \\ \text{increasing width increases } \Delta L & f < f_{c1} \end{array}$$

(iv) thickness of floating plate:

$$\begin{array}{ll} \Delta L \sim 30 \log h_1 & f > f_{c1} \\ \text{increasing thickness increases } \Delta L & f < f_{c1} \end{array}$$

(v) quality of interlayer:

$$\begin{array}{ll} \text{very important} & f > f_{c1} \\ \text{probably no influence} & f < f_{c1} \end{array}$$

Ljunggren also reports three different full scale measurements on 100 mm concrete slabs "floating" on an interlayer of 100 mm of rockwool (150 kg/m^3). The results show surprisingly low insulation improvements, much lower than expected from the model measurements. Ljunggren assures that no sound bridges exist, and states that the most probable explanation lies in some different properties of the interlayer.

In 1980 Kuhl [20] discusses floating floors from a somewhat different view. He starts by discussing the excitation of a plate through a spring, and points out the different behaviour of the plate at low and high frequencies. At low frequencies, below the plates first flexural mode, it behaves like a lumped mass, which gives a frequency variation of 40 dB/decade for the difference in velocity level between plate and exciter. At higher frequencies, when the plate has a resonant field of flexural modes, this frequency variation is 30 dB/decade, and for an infinite plate the variation

is 20 dB/decade. Kuhl also points out the different behaviour of the spring, below and above its first resonance frequency, but does not discuss the practical implication of this any further. His model measurements are rather difficult to interpret, but he also makes a study on full-scale measurements of floating floors with continuous interlayers. He divides those into two separate cases of large floors and small floors respectively and normalizes all the improvement curves to 20 dB at 500 Hz. The small floors are found to have a slope of 40 dB/decade, whereas the large ones have 20 dB/decade in average, except perhaps at low frequencies, where the slope is steeper. Kuhl finds it surprising that the small floors have such a steep curve, even at frequencies where their flexural mode density is high.

In 1981 Lindblad [21] suggests that to the forced transmission in Cremer's theory, one may add a resonant transmission, which may be estimated as follows: The structural slab vibrates with a long wavelength as compared to the free vibrations in the structural slab. This situation is similar to that of airborne sound exciting a finite wall at $f < f_c$, where coupling takes place over a width around the edges corresponding to a quarter of the bending wavelength. Analogously Lindblad suggests as coupling length a quarter of the floating slab's wavelength, and introduces the following correction term:

$$\Delta\Delta L_{\text{res}} = -10 \log \left(1 + \frac{\lambda_{B1}}{4\eta_1} \frac{U}{S} \sigma_1 \right) \quad (6)$$

In 1983 Lindblad [22] suggests a slightly different correction term. Now he assumes that $1/\pi$ of the forced waves in the floating slab are reflected or scattered at the edges U as free waves with the power $2c_{B1} U m_1 \cdot \tilde{v}_{\text{forced}}^2 / \pi$.

This power is dissipated as $\omega \eta_1 m_1 S \cdot \tilde{v}_{\text{free}}^2$, and the power balance produces $\tilde{v}_{\text{free}}^2 / \tilde{v}_{\text{forced}}^2 = \lambda_{B_1} U / (\pi^2 \eta_1 S)$. Now assuming the radiation efficiency of the forced waves being 1 and that of the free waves being σ_1 the correction term here becomes:

$$\Delta \Delta L_{\text{res}} = -10 \log \left(1 + \frac{\tilde{v}_{\text{free}}^2}{\tilde{v}_{\text{forced}}^2} \sigma_1 \right) \quad (7)$$

or

$$\Delta \Delta L_{\text{res}} = -10 \log \left(1 + \frac{\lambda_{B_1} U}{\pi^2 \eta_1 \cdot S} \sigma_1 \right) \quad (8)$$

In 1982 Widén [23] discusses the use of concrete tiles instead of a homogeneous concrete slab in floating floors. He refers to Kuhl [20] and states that by this subdivision of the floor, the first flexural mode is moved to a much higher frequency. Each tile should therefore behave as a lumped mass on a spring and this should improve the sound insulation of the floating floor. Widén reports model measurements that confirm the above statement. He also compares several measurements with a calculation model and finds good agreement. No detailed description of the calculation model is included in the report, but Widén himself explains that he has assumed the forced waves to be reflected at the edges as free waves with the same amplitude as the forced ones. This should then probably give a correction term similar to equation (8), possibly with minor differences depending on the detailed assumptions.

In this historical review, many publications on floating floors have now been cited. Several others have been studied without citing them here directly. The material in these publications, either does not include much on resonant transmission, or is already included in some of the cited publications above.

It may however be appropriate to mention some of these here. In [24] Brekke discusses briefly the "calculation methods" of Cremer, Vêr, Nilsson and Kuhl. He then reports measurements on 7 different floating floor constructions in technical rooms which means that the floating slabs are rather heavy (100 mm of concrete). The measurements are made on the total constructions only, and then the insulation improvement is calculated backwards. He gets the highest insulation improvement for floating floors with separate mountings, and for these the agreement with the calculations of Vêr is good at low frequencies. At 500 - 1000 Hz the steep slope of the curves is however drastically reduced. For the floors with continuous mineral wool Brekke gets two groups of results. In one group the curves follow the expression $20 \log (f/f_1)$ reasonably well, but in the other group the results are much lower and have a less steep slope, about $10 \log (f/f_1)$.

In [25] Nelson discusses floating floors as vibration isolation (in technical rooms), and the effect of an additional resilient layer and blocking mass.

In [26] Cremer and Gilg compare the theory for resilient surface layers to Cremer's theory of locally reacting floating floors, but nothing new is added to it.

In [27] a large series of measurements on light floating floors is reported. Both transmission loss and impact insulation are measured for the total constructions, but neither ΔR nor ΔL is measured.

3.2 Existing solutions, further discussions

Although much has been written about resonant transmission, only a few attempts have been made to estimate this quantitatively.

We have two SEA formulations, one by VÉR [2], [3], [4] but his interlayer is modelled as separate resilient mounts, and one by Ljunggren [18], but his interlayer is modelled as an airspace lined with an absorbent. Neither model applies particularly well to the problem of a continuous interlayer.

Remaining is only the work of Nilsson [10] and the approximate formulas by Lindblad in [21] and [22]. Widéns formula is not available but it is probably similar to Lindblad's formulas.

Let us first take a closer look at Nilsson's work. We have already described his model, which is an one-dimensional beam model as shown in the figure below.

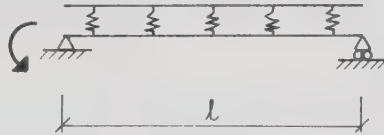


Figure 3.2.1. Nilsson's model.

In his calculations, Nilsson starts with the coupled homogeneous bending wave equations (see 2.1.3), but he simplifies the coupling term by assuming $|v_1| \ll |v_3|$ (here plate 3 is excited). He then continues with the new wavenumbers

$$\kappa_i^4 = (\omega^2 m_i - K) / B_i \quad (1)$$

for the coupled bending waves.

Using the boundary conditions for the bottom plate, he derives an expression for its lateral displacement

and finds the frequency and space average of the velocity. In space this is done by a normalized integration along the beamlength and in frequency by integrating over one resonance top, normalized with the interval between two such resonances. The motion of the top plate is divided into a forced part and a free part. By using the same method as for the bottom plate, with the different boundary conditions, frequency and space averages of the free and forced vibrations of the top plate are found. This is done for three separate cases: $\kappa_1 \ll \kappa_3$, $\kappa_1 \gg \kappa_3$ and $\kappa_1 = \kappa_3$

In his next step, Nilsson introduces losses, both in the floating slab ($\eta_{1,0}$) and in the spring (η_2) by writing (1) with a complex bending stiffness and a complex spring constant:

$$\kappa_1^4 = \frac{\omega^2 m_1 - K(1 + j\eta_2)}{B_1(1 + j\eta_{1,0})} \quad (2)$$

or

$$\kappa_1 = k_{B_1} \left[\frac{1 - (\omega_1/\omega)^2 - j\eta_2(\omega_1/\omega)^2}{1 + j\eta_{1,0}} \right]^{1/4} \quad (3)$$

$$\approx k_{B_1} [1 - (\omega_1/\omega)^2]^{1/4} \cdot (1 - j\eta_{1,tot})^{1/4} \quad (4)$$

where

$$\eta_{1,tot} \approx \eta_{1,0} + \eta_2 \frac{\omega_1^2}{\omega^2 - \omega_1^2} \quad (5)$$

$$\text{and } \omega_1^2 = K/m_1 \quad (6)$$

For the forced transmission (and $\kappa_1 \gg \kappa_2$ or $\kappa_1 \ll \kappa_2$) Nilsson gets the same solution as we get from Cremer's model, when the structural slab is excited (see part 2.2):

$$\Delta L_{v,forced} = 40 \log(f/f_1) + 20 \log |(f_{c_3}/f_{c_1})^2 - 1| \quad (7)$$

The resonant transmission is somewhat different for each of the cases ($\kappa_1 \ll \kappa_3$) and ($\kappa_1 \gg \kappa_3$). If we write this resonant transmission as a correction term to the forced transmission we get:

$$\Delta L_V = \Delta L_{V, \text{forced}} + \Delta \Delta L_{V, \text{res}} \quad (8)$$

where

$$\Delta \Delta L_{V, \text{res}} = -10 \log \left[1 + \left(\frac{\kappa_3}{\kappa_1} \right)^6 \frac{6}{\kappa_1 \ln \eta_{1, \text{tot}}} \right] \quad (\kappa_1 \gg \kappa_3) \quad (9)$$

$$\Delta \Delta L_{V, \text{res}} = -10 \log \left[1 + \left(\frac{\kappa_3}{\kappa_1} \right)^3 \frac{1}{\kappa_1 \ln \eta_{1, \text{tot}}} \right] \quad (\kappa_1 \ll \kappa_3) \quad (10)$$

Now already at a frequency 1/3-octave above f_1 we have $\kappa_1 = 0.77 k_{B_1}$ and one octave above f_1 we have $\kappa_1 = 0.93 k_{B_1}$. As κ_3 converges even faster we can replace κ with the expression:

$$\kappa \approx k_B = (2\pi/c) \sqrt{ff_c} \quad (f_1 < f) \quad (11)$$

We also have the expression (5) for $\eta_{1, \text{tot}}$, and Nilsson assumes that $\eta_{1,0}$ can be neglected for a lightly damped plate, giving:

$$\eta_{1, \text{tot}} \approx \eta_2 \left(\frac{\omega_1}{\omega} \right)^2 \frac{1}{1 - (\omega_1/\omega)^2} \approx \eta_2 (\omega_1/\omega)^2 \quad (12)$$

Finally Nilsson replaces 1 with $2S/U$ for a two-dimensional plate, and the expressions (9) and (10) can be written as:

$$\Delta \Delta L_{V, \text{res}} \approx -10 \log \left[1 + \left(\frac{f_{c3}}{f_{c1}} \right)^3 \frac{\lambda_{B_1} U}{(2\pi/3) \eta_2 S} \left(\frac{\omega}{\omega_1} \right)^2 \right] \quad (\kappa_1 \gg \kappa_3) \quad (13)$$

$$\Delta \Delta L_{V, \text{res}} \approx -10 \log \left[1 + \left(\frac{f_{c3}}{f_{c1}} \right)^{3/2} \frac{\lambda_{B_1} U}{4\pi \eta_2 S} \left(\frac{\omega}{\omega_1} \right)^2 \right] \quad (\kappa_1 \ll \kappa_3) \quad (14)$$

We see here that as $\lambda_{B_1} \sim f^{-1/2}$ we have a frequency dependance $\sim f^{3/2}$, so the total velocity level

improvement (both forced and resonant transmission considered) will have the frequency dependence:

$$\Delta L_v \sim 10 \log f^{5/2} = 25 \text{ dB/decade} \quad (15)$$

This is often cited as the slope of the velocity level improvement deduced by Nilsson.

However if we take a closer look at the expression (13) which describes the most usual configuration in building acoustics, we find that the resonant term must of course be larger than 1 if it is to be significantly noticed. For plates with extremely different κ and thus f_c , such as a 16 cm concrete slab and a 22 mm chip board, we find $f_{c_3}/f_{c_1} \approx 0.1$. For a typical $f_1 = 80 \text{ Hz}$, the resonant term is now less than 1 over the entire frequency region of building acoustics, and the resonant transmission of equation (13) is thus negligible for this construction. However if we consider concrete slabs of 5 and 16 cm respectively we have instead $f_{c_3}/f_{c_1} \approx 0.3$ and we find the opposite relation, that the resonant term may be larger than 1 over a large part of the frequency region of building acoustics.

Another observation can be made regarding the simplification of equation (5), when $\eta_{1,0}$ is neglected as compared to $\eta_2 (\omega_1/\omega)^2$.

Let us assume that $\eta_{1,0}$ is a material constant, as the floating plate has no connection to other structural elements except the resilient layer. This would mean a numerical value of around 1 - 3 % for many common building materials (chip board, concrete slab etc) and it can be assumed to be almost frequency independent. Now η_2 for mineral wool is reported to be 20 - 30 % at low frequencies. If we assume η_2 to be independent of frequency as well, we see that at a frequency two octaves above f_1 the two terms in equation (5) are of the same

order of magnitude, and three octaves above f_1 we can just about neglect the term $\eta_2 (\omega_1/\omega)^2$. (Besides η_2 may decrease with increasing frequency.)

The conclusion is that in practice the equations (13) and (14) are only valid in the frequency region of one or two octaves above f_1 .

However, the equations (9) and (10) are more general and should be used instead of the simplified equations (13) and (14).

Nilsson's measurements are made on floating plates of chip board and a steel plate. The steel plate may have lower inner losses than estimated above, but even if $\eta_{1,0}$ is lowered one order of magnitude, the valid frequency region is only stretched up to about 3 octaves above f_1 . Nilsson's results, however, show good agreement with his expression corresponding to (13) and (14) up to 7 octaves above f_1 , both for the steel plate and for the chip board. The conclusion must be that another phenomenon is responsible for the experienced frequency variation of 20 - 25 dB/decade. It is probably the behaviour of the resilient layer as a wave medium that is responsible, especially as Nilsson uses thick layers of mineral wool. (See chapter 4.)

Summing up Nilsson's results, we have the same "forced" solution for the difference in the velocity level as the one deduced from Cremer's theory, when the structural slab is excited. This difference in velocity level is equal to the insulation improvement as previously stated. If we write the resonant transmission as a correction term to this forced transmission, we have equations (9) and (10) for the difference between resonant and forced vibrations.

If the radiation efficiencies are called σ_{res} and σ_{forced} respectively we may write:

$$\Delta\Delta L_{\text{res}} = -10 \log \left(1 + \left(\frac{f_{c_3}}{f_{c_1}} \right)^3 \frac{\lambda_{B_1} U}{(2\pi/3) \eta_1 S} \left(\frac{\sigma_{\text{res}}}{\sigma_{\text{forced}}} \right) \right) \quad (k_{B_1} \gg k_{B_3}) \quad (16)$$

$$\Delta\Delta L_{\text{res}} = -10 \log \left(1 + \left(\frac{f_{c_3}}{f_{c_1}} \right)^{3/2} \frac{\lambda_{B_1} U}{4\pi \eta_1 S} \left(\frac{\sigma_{\text{res}}}{\sigma_{\text{forced}}} \right) \right) \quad (k_{B_1} \ll k_{B_3}) \quad (17)$$

We no longer have reciprocity in that meaning that we can keep the excitation fixed and let the plates change places, as they here have different boundary conditions. However reciprocity must still be valid in its actual meaning, and we should still have the same result for excitation of plate 1 as the above result for excitation of plate 3.

Note that, in order to simplify the notation, we have here written η_1 instead of $\eta_{1,\text{tot}}$ but the meaning is the same, η_1 includes both inner losses, and losses due to coupling to other construction elements.

If we now take another look at Lindblad's approximate formulas, we see that they are similar to Nilsson's expressions, when written as in (16) and (17):

$$\Delta\Delta L_{\text{res}} = -10 \log \left(1 + \frac{\lambda_{B_1} U}{4\eta_1 S} \sigma_1 \right) \quad (18)$$

$$\Delta\Delta L_{\text{res}} = -10 \log \left(1 + \frac{\lambda_{B_1} U}{\pi^2 \eta_1 S} \sigma_1 \right) \quad (19)$$

Both equations are deduced considering the case when $k_{B_1} \gg k_{B_3}$, and are thus comparable to Nilsson's equation (16). Lindblad has also assumed $\sigma_{\text{forced}} \approx 1$ and $\sigma_{\text{res}} \approx \sigma_1$, the usual radiation efficiency for point-excited panels.

The resonant term in (18) and (19) is larger than that in (16), especially if the slabs are very dissimilar. If we have two concrete slabs 50 and 160 mm, we have $(f_{c_3}/f_{c_1})^3 = 1/33$, so here Lindblad's resonant term in (19) is 7 times larger than Nilsson's. If we have a 22 mm chip board and the same 160 mm concrete slab, Lindblad's resonant term is 200 times larger!

We may ask why the results of Lindblad (and Widén) differ so much from the results of Nilsson. Which result is more correct?

The basic difference between the two methods lies in the fact that Nilsson assumes the structural slab to be simply supported, i.e. transverse movement is prevented at the edge.

The scattering model used by Lindblad (and Widén) does not have this limitation at the edges.

If we consider an example where the structural slab and the floating slab are of equal size the scattering model corresponds best to the case of a structural slab with "guided" edges. (The edge is free to displace laterally, but rotation is prevented.)

This is, of course, not a very realistic boundary condition, and Nilsson's result should apply better here. The boundary conditions may not be simply supported exactly, but even for a clamped edge Nilsson's result may be considered the more correct one in this situation of equally sized slabs.

However, if we consider another case, where the floating slab is smaller than the structural slab, we may come to a different conclusion. This is a common situation e.g. in machine halls, where machines are placed on a floating slab, which covers

only a part of the structural floor. We may also consider the case of several rooms on one large continuous structural slab. These rooms are separated by lightweight walls and may have individual floating floors. In these situations Lindblad's scattering model may give a more correct result, as here the structural slab is far from being simply supported around the edges of the floating slab. It can both rotate and move transversally.

In parts 3.3 and 3.4 that follow, we confine ourselves to the case of equally sized slabs. These may however have different boundary conditions.

3.3 Solution through coupling integrals

3.3.1 Method and model

In [28] Lindblad uses the method of coupling integrals to evaluate the coupling between an exciting airborne sound field in a room and the vibrational field in a wall. This work has been studied and an analog method developed to solve the problem of coupling between the two plates in a floating floor.

Our model is a primarely excited structural slab, and a floating slab, which is coupled to the structural slab through a locally reacting interlayer. We also assume $k_{B_3} \ll k_{B_1}$. The vibrational field in the structural slab can be written as:

$$v_3(x, z) = \sum_N v_{3N} \psi_{3N}(x, z) \quad (1)$$

N is an array of two numbers, and the eigenfunctions ψ_{3N} are solutions of the homogeneous bending wave equation and must also satisfy the boundary conditions.

We only consider one mode in the structural slab at a time: $v_{3N}, \psi_{3N}(x, z)$. This mode acts on the floating slab through the locally reacting interlayer, and the pressure distribution can be written as:

$$p(x, z) = \frac{K}{j\omega} v_{3N} \psi_{3N}(x, z) \quad (2)$$

The vibrational field of the floating slab can be written in analogy to (1):

$$v_1(x, z) = \sum_N v_{1N} \psi_{1N}(x, z) \quad (3)$$

This vibrational field must satisfy the inhomogeneous bending wave equation for the floating slab:

$$\frac{B_1}{j\omega} (\nabla^4 - k_{B_1}^4) v_1(x, z) = p(x, z) \quad (4)$$

After insertion of equation (3) into (4), multiplying both sides with a single eigenfunction ψ_N , and integrating over the area we get:

$$v_1(x, z) = \sum_N \frac{F_N \psi_{1N}}{\frac{B_1}{j\omega} (k_N^4 - k_{B_1}^4)} \quad (5)$$

where

$$F_N = \frac{\iint_S p(x, z) \psi_{1N} dS}{\iint_S \psi_{1N}^2 dS} \quad (6)$$

Introducing losses by writing $k_{B_1}^4 (1 - j\eta)$ and rewriting (5) we also have:

$$v_1(x, z) = \frac{1}{-j\omega m} \sum_N \frac{F_N \psi_{1N}}{(f_N/f)^2 - 1 + j\eta} \quad (7)$$

It is convenient to divide this equation into two parts. One with low modenumbers, so that $f_N < f$, and the other with high modenumbers.

For the first case $f_N < f$, we can disregard the term $j\eta$ in the denominator which will have almost the same value for the different modes. The only terms that give significant contribution in F_N are those with similar waveforms as the exciting mode in the structural slab ($\psi_{1N} \approx \psi_{3N}$).

If the slabs have identical boundary conditions (and the same size), we have the same series of eigenfunctions in both slabs. The function F_N now becomes zero, except for the single term which has the same eigenfunction as the exciting mode, and for this term we have $F_N = v_{3N} \cdot K/(j\omega)$. This means:

$$v_{1N} = \frac{K}{\omega^2 m} \frac{v_{3N}}{(k_{B_3}/k_{B_1})^4 - 1} \quad (8)$$

Averaging over the area gives our previously deduced forced solution:

$$\langle v_{1, \text{forced}}^2 \rangle = (\omega_1/\omega)^4 \frac{\langle v_3^2 \rangle}{[(k_{B_3}/k_{B_1})^4 - 1]^2} \quad (9)$$

More generally the two slabs have different boundary conditions and different eigenfunctions.

To obtain the forced solution in this general case, we have to sum up the terms that have the strongest coupling (ψ_{1N} closest to ψ_{3N}), but the result will be the same.

The remainder of the sum in (7) is of a different character. Here we obviously get a maximum, a resonance top, at every frequency where $f \approx f_N$. At the resonance we get a very small denominator, but the coupling in the numerator is much smaller than before:

$$F_N'' = \frac{K}{j\omega} \frac{v_{3N}' \int_S \psi_{3N}' \psi_{1N}'' dS}{\int_S \psi_{1N}''^2 dS} \quad (10)$$

If the eigenfunctions in the numerator are sufficiently different, than the coupling integral over the area cancels out. Only some uncanceled remainders are left at edges and corners (assuming that the boundary conditions are dissimilar).

We now consider a frequency band Δf , which contains a certain number of resonances or modes, the modal density being $(\Delta N/\Delta f)$. Instead of summing over all the resonance tops in the frequency band, we consider the power response of one typical top, and sum up by multiplying with the modal density, which then gives us the average frequency response. The equivalent power bandwidth (see e.g. [29]) can be written as:

$$\Delta_e = f\eta\pi/2 \quad (11)$$

The top value of one resonance is determined by η , and we have:

$$\langle v_{1,res}^2 \rangle = \left(\frac{K}{\omega^2 m} \right) \frac{\langle v_3^2 \rangle C^2}{\eta^2} \Delta_e \left(\frac{\Delta N}{\Delta f} \right) \quad (12)$$

with

$$C = \frac{\iint_S \psi_3 \psi_1 dS}{\iint_S \psi_1^2 dS} \quad (13)$$

The flexural mode density for a plate and a beam respectively (see e.g. [6]) is:

$$\left(\frac{\Delta N}{\Delta f} \right)_2 = \frac{k_B^2 S}{4\pi f} \quad (14)$$

$$\left(\frac{\Delta N}{\Delta f} \right)_1 = \frac{k_B l}{2\pi f} \quad (15)$$

As previously stated the coupling of equation (13) cancels out except at the edges and corners.

We omit the corner coupling and calculate only the coupling at the edges. Taking one edge at a time, we assume the plate to be semi-infinite, and integrate from an edge and inwards. In order to get a converging solution we introduce $e^{\delta x}$ with $\delta \ll 1$. This does not affect the result at the edge but gives zero at minus infinity.

Our coupling integral now turns out to be:

$$w = \left| \int_0^{-\infty} e^{\delta x} \psi_1'(x) \psi_3'(x) dx \right| \quad (16)$$

Here ψ' stands for the normalized modes:

$$\psi' = \frac{1}{2} \psi \quad (17)$$

w has the dimension of a length and it can be regarded as the width of a coupling strip at the edge. The coupling C can now be found by comparing the area of the coupling strips to the total area of the plate.

3.3.2 One-dimensional "beam model"

Let us first consider the relatively simple case of coupling between two beams (or normal incidence in a plate).

Using the notation of Cremer in [6], we can write for a free bending wave incident on a boundary at $x = 0$ from the $-x$ direction:

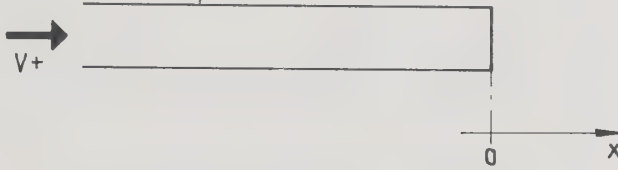


Figure 3.3.2.1. Beam model with a boundary at $x = 0$.

$$v = v_+ (e^{-jk_B x} + r e^{+jk_B x} + r_j e^{+k_B x}) \quad (18)$$

The four field variables v , w , M , F are related by the following cyclical differential equations:

$$(i) \quad w = \frac{\partial v}{\partial x}$$

$$(ii) \quad M = -\frac{B}{j\omega} \frac{\partial w}{\partial x}$$

$$(iii) \quad F = -\frac{\partial M}{\partial x}$$

$$(iv) \quad v = -\frac{1}{j\omega m} \frac{\partial F}{\partial x}$$

(See e.g. [6], also used in [30].)

If the beam is clamped at the end we have the two boundary conditions $v = 0$ and $w = 0$, which gives:

$$\begin{bmatrix} 1 & 1 \\ j & 1 \end{bmatrix} \begin{bmatrix} r \\ r_j \end{bmatrix} = \begin{bmatrix} -1 \\ j \end{bmatrix} \quad (19)$$

This means that (18) can be written as:

$$v = v_+ (e^{-jk_B x} - j e^{+jk_B x} + (-1+j) e^{+k_B x}) \quad (20)$$

The mode form at a clamped end is thus:

$$\psi_C = e^{-jk_B x} - j e^{+jk_B x} + (-1+j) e^{+k_B x} \quad (21)$$

Similarly we find for a simply supported end, where $v = 0$ and $M = 0$:

$$\begin{bmatrix} 1 & 1 \\ -1 & 1 \end{bmatrix} \begin{bmatrix} r \\ r_j \end{bmatrix} = \begin{bmatrix} -1 \\ 1 \end{bmatrix} \quad (22)$$

and

$$\psi_S = e^{-jk_B x} - e^{+jk_B x} \quad (23)$$

For a free end we have $M = 0$ and $F = 0$, which gives:

$$\begin{bmatrix} -1 & 1 \\ -j & 1 \end{bmatrix} \begin{bmatrix} r \\ r_j \end{bmatrix} = \begin{bmatrix} 1 \\ -j \end{bmatrix} \quad (24)$$

and

$$\psi_F = e^{-jk_B x} - j e^{+jk_B x} + (1-j) e^{+k_B x} \quad (25)$$

Let us first assume that the structural slab is simply supported. Now if the floating slab is also simply supported we get

$$w_{SS} = \left| \frac{1}{4} \int_0^{-\infty} e^{+\delta x} \psi_{S1} \psi_{S3} dx \right| \quad (26)$$

After insertion of equation (23) we find:

$$w_{SS} = \left| \int_0^{-\infty} e^{\delta x} \sin(k_{B3} x) \sin(k_{B1} x) dx \right| = 0 \quad (27)$$

These boundary conditions are not found to give any coupling, and thus there are no free or resonant waves excited in the floating slab at the edges. This has

previously been shown by Cremer and Heckl [7] as mentioned before.

Now we consider the more realistic case of a free floating slab. The structural slab is still assumed to be simply supported. Here we get:

$$w_{SF} = \left| \frac{1}{4} \int_0^{-\infty} e^{\delta x} \psi_{F1} \psi_{S3} dx \right| \quad (28)$$

After insertion of equations (23) and (25) we find:

$$w_{SF} = \frac{1}{\sqrt{2}} \left| \int_0^{-\infty} e^{\delta x} \sin(k_{B3} x) \cos(k_{B1} x) dx + \int_0^{-\infty} e^{k_{B1} x} \sin(k_{B3} x) dx \right| \quad (29)$$

or

$$w_{SF} = \sqrt{2} \left| \frac{k_{B3}^3}{k_{B3}^4 - k_{B1}^4} \right| \quad (30)$$

The coupling C can now be evaluated as:

$$C_{SF} = \frac{2w_{SF}}{1} = \frac{\sqrt{8}}{1k_{B1}} \left| \frac{(k_{B3}/k_{B1})^3}{(k_{B3}/k_{B1})^4 - 1} \right| \quad (31)$$

Inserted into equation (12) together with equations (9), (11) and (15) this gives:

$$\begin{aligned} Y_{SF} &= \frac{\langle v_{1,res}^2 \rangle}{\langle v_{1,forced}^2 \rangle} = \frac{1}{\eta^2} \frac{8}{1^2 k_{B1}^2} \left(\frac{k_{B3}}{k_{B1}} \right)^6 \left(\frac{f\eta\pi}{2} \right) \left(\frac{k_{B1}}{2\pi f} \right) \\ &= \frac{2}{1k_{B1}\eta} \left(\frac{k_{B3}}{k_{B1}} \right)^6 \end{aligned} \quad (32)$$

This case of a simply supported structural slab and a free - free floating slab in one dimension is just the one studied by Nilsson. His result was written as equation (3.2.9), and we see that the results are identical except for the numerical constant, which is 2 here, but Nilsson has 6. The two methods should of course give the same result.

We assume next the case of a clamped structural slab, and a free floating slab. This gives

$$w_{CF} = \left| \frac{1}{4} \int_0^{-\infty} e^{\delta x} \psi_{F1} \psi_{C3} dx \right| \quad (33)$$

After insertion of equations (21) and (25) we find:

$$w_{CF} = \frac{1}{2} \left| \int_0^{\infty} e^{\delta x} (\cos(k_{B3}x) + \sin(k_{B3}x) - e^{k_{B3}x}) \cdot (\cos(k_{B1}x) + \sin(k_{B1}x) + e^{k_{B1}x}) dx \right| \quad (34)$$

$$w_{CF} = 2 \left| \frac{k_{B3}^2 (k_{B1} - k_{B3})}{k_{B1}^4 - k_{B3}^4} \right| \quad (35)$$

The coupling can now be evaluated as:

$$C_{CF} = \frac{2w_{CF}}{1} = \frac{4}{1k_{B1}} \left| \frac{(k_{B3}/k_{B1})^2 ((k_{B3}/k_{B1}) - 1)}{(k_{B3}/k_{B1})^4 - 1} \right| \quad (36)$$

As before we calculate the resonant field in terms of the forced field:

$$\begin{aligned} Y_{CF} &= \frac{\langle v_1^2, \text{res} \rangle}{\langle v_1^2, \text{forced} \rangle} = \frac{1}{\eta^2} \frac{16}{1^2 k_{B1}^2} \left(\frac{k_{B3}}{k_{B1}} \right)^4 \left(\frac{k_{B3}}{k_{B1}} - 1 \right)^2 \frac{k_{B1} 1\eta}{4} \\ &= \frac{4}{1\eta k_{B1}} \left(\frac{k_{B3}}{k_{B1}} \right)^4 \left(\frac{k_{B3}}{k_{B1}} - 1 \right)^2 \end{aligned} \quad (37)$$

We would now like to extend the above results to two dimensions, for a plate. We do this at first without calculating for oblique incidence, and get a result comparable to that of Nilsson [10]. To make this extension, we consider at first Snell's law along the edge, and make the assumption that in spite of a diffuse wave field in the structural slab, the waves in the thinner floating slab will be almost perpendicular to the edge. Another assumption is that the one-dimensional coupling calculated above is a good estimate for the diffuse coupling.

(In part 3.4 below we continue the work and consider the influence of oblique incidence.)

Now we may define the area of the plate as ab . The coupling in one direction is $2w/b$ and in the other direction $2w/a$. As the free wavefield is assumed to be almost normal to the edge, we still use the same modal density $(\Delta N/\Delta f)_1$ from equation (15). We must also consider that the area mean square of the forced field is $1/2$ of the edge mean square. Summing up we get:

$$\begin{aligned}
 Y_{SF} &= \frac{1}{\eta^2} \left(\frac{8}{a^2 k_{B_1}^2} \frac{k_{B_1} a \eta}{4} + \frac{8}{b^2 k_{B_1}^2} \frac{k_{B_1} b \eta}{4} \right) \left(\frac{k_{B_3}}{k_{B_1}} \right)^6 \cdot 2 \\
 &= \frac{4}{k_{B_1} \eta} \left(\frac{1}{a} + \frac{1}{b} \right) \left(\frac{k_{B_3}}{k_{B_1}} \right)^6 \\
 &= \frac{2U}{k_{B_1} \eta S} \left(\frac{k_{B_3}}{k_{B_1}} \right)^6
 \end{aligned} \tag{38}$$

And analogously:

$$Y_{CF} = \frac{4U}{k_{B_1} \eta S} \left(\frac{k_{B_3}}{k_{B_1}} \right)^4 \left(\frac{k_{B_3}}{k_{B_1}} - 1 \right)^2 \tag{39}$$

We see that our two-dimensional solution (38) is almost the same as Nilsson's, see equation (3.2.16). His numerical constant is 3 instead of our 2.

For most practical cases the floating slab can be assumed to have all edges free. The structural slab can be assumed to have a certain degree of clamping, varying between being simply supported and totally clamped. The resonant term should therefore have a value somewhere between the expressions (38) and (39).

If we consider the case of a 50 mm floating concrete slab and a 160 mm structural slab, we have:

$$Y_{SF} \approx \frac{\lambda_{B_1} U}{100 \cdot \eta S}$$

and

$$Y_{CF} \approx \frac{\lambda_{B1} U}{80 \cdot \eta S}$$

For more dissimilar plates we have larger difference in Y_{SF} and Y_{CF} , and $Y_{CF} > Y_{SF}$.

We consider the same structural slab of 160 mm concrete with a 22 mm chip board as a floating plate which gives:

$$Y_{SF} \approx \frac{\lambda_{B1} U}{3000 \cdot \eta S}$$

$$Y_{CF} \approx \frac{\lambda_{B1} U}{300 \cdot \eta S}$$

On the other hand if the plates are more similar, we have $Y_{CF} < Y_{SF}$. A practical example is a 200 mm slab of lightweight concrete and a 50 mm slab of dense concrete, where $f_{c1}/f_{c3} \approx 2$. Here we have

$$Y_{SF} \approx \frac{\lambda_{B1} U}{25 \cdot \eta S}$$

$$Y_{CF} \approx \frac{\lambda_{B1} U}{75 \cdot \eta S}$$

We see that all the practical cases discussed here have a smaller resonant term than the suggested correction term by Lindblad. (Equations (3.2.18) and (3.2.19).)

At the end of part 3.2 we suggested that Lindblad's scattering model would correspond best to a structural slab with guided edges in the case of two equally sized slabs.

For a beam with a guided end we have $w = 0$ and $F = 0$, which gives:

$$\psi_G = e^{-jk_B x} + e^{+jk_B x} \quad (39a)$$

The coupling width becomes:

$$\begin{aligned}
 w_{GF} &= \frac{1}{4} \left| \int_0^{-\infty} e^{\delta x} \psi_{F_1} \psi_{G_3} dx \right| \\
 &= \frac{\sqrt{2}}{k_{B_1}} \left| \frac{(k_{B_3}/k_{B_1})^2}{(k_{B_3}/k_{B_1})^4 - 1} \right|
 \end{aligned} \tag{39b}$$

and the one-dimensional ratio Y:

$$Y_{GF} = \frac{2}{1k_{B_1}\eta} \left(\frac{k_{B_3}}{k_{B_1}} \right)^4 \tag{39c}$$

Extended into two dimensions this becomes:

$$Y_{GF} = \frac{2U}{k_{B_1}\eta S} \left(\frac{k_{B_3}}{k_{B_1}} \right)^4 \tag{39d}$$

For the case when $f_{C_1}/f_{C_3} \approx 2$ we get an almost identical result as Lindblad in (3.2.19).

However we still have the ratio of the wave numbers included in Y, which then decreases with decreasing similarity between the two slabs. The case of the 50 mm and 160 mm concrete slabs gives for example about 3 times lower Y. Still the guided solution is closer to the scattering model than the ones for a simply supported or clamped solutions are.

3.3.3 Oblique incidence

Let us consider a free bending wave in a plate, that lies in the xz -plane. The wave arrives from ($x < 0$, $z > 0$) and moves towards the edge of the plate, along which we have placed the z -axes. (See figure below.) The angle of incidence is θ .

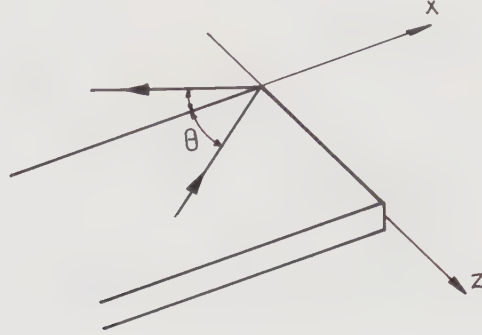


Figure 3.3.3.1. Plate model with an edge along the z -axes.

The primary bending wave results in a reflected wave and a nearfield:

$$v = v_+ \left[e^{-jk_B \cos \theta x} + r e^{jk_B \cos \theta x} + r_j e^{k_B \sqrt{1 + \sin^2 \theta} x} \right] e^{jk_B \sin \theta z} \quad (40)$$

(The factor $e^{jk_B \sin \theta z}$ has been omitted.)

Besides v we have the following three field variables along the edge and they can be written in terms of v as:

$$(i) \quad w = \frac{\partial v}{\partial x}$$

$$(ii) \quad M = -\frac{B}{j\omega} \left[\frac{\partial^2 v}{\partial x^2} - \mu (k_B \sin \theta)^2 v \right]$$

$$(iii) \quad F = \frac{B}{j\omega} \left[\frac{\partial^3 v}{\partial x^3} - (2 - \mu) (k_B \sin \theta)^2 \frac{\partial v}{\partial x} \right]$$

(see e.g. [6], also used in [30]).

If we first assume the plate to be simply supported at the edge, we have $v = 0$ and $M = 0$ which gives:

$$\begin{bmatrix} 1 & 1 \\ -\alpha & \beta \end{bmatrix} \begin{bmatrix} r \\ r_j \end{bmatrix} = \begin{bmatrix} -1 \\ \alpha \end{bmatrix} \quad (41)$$

$$\text{where } \alpha = 1 - (1 - \mu) \sin^2 \theta \quad (42)$$

$$\beta = 1 + (1 - \mu) \sin^2 \theta \quad (43)$$

This means that $r = -1$ and $r_j = 0$, and the mode form in the x -direction at a simply supported end can be written as:

$$\psi_S(x) = e^{-jk_B \cos \theta x} - e^{+jk_B \cos \theta x} \quad (44)$$

For a free edge we have $M = 0$ and $F = 0$, which gives:

$$\begin{bmatrix} -\alpha & \beta \\ -j\gamma\beta & \alpha\varepsilon \end{bmatrix} \begin{bmatrix} r \\ r_j \end{bmatrix} = \begin{bmatrix} \alpha \\ -j\gamma\beta \end{bmatrix} \quad (45)$$

$$\text{where } \gamma = \sqrt{1 - \sin^2 \theta} = \cos \theta \quad (46)$$

$$\varepsilon = \sqrt{1 + \sin^2 \theta} \quad (47)$$

Thus the mode form corresponding to the free edge is:

$$\psi_F(x) = e^{-j\gamma k_B x} + [r]_F e^{j\gamma k_B x} + [r_j]_F e^{\varepsilon k_B x} \quad (48)$$

where

$$[r]_F = \frac{\varepsilon\alpha^2 + j\gamma\beta^2}{-\varepsilon\alpha^2 + j\gamma\beta^2} \quad (49)$$

$$[r_j]_F = \frac{2j\alpha\beta\gamma}{-\varepsilon\alpha^2 + j\gamma\beta^2} \quad (50)$$

Finally we consider the clamped edge, where $v = 0$ and $w = 0$. This gives

$$\begin{bmatrix} 1 & 1 \\ j\gamma & \varepsilon \end{bmatrix} \begin{bmatrix} r \\ r_j \end{bmatrix} = \begin{bmatrix} -1 \\ j\gamma \end{bmatrix} \quad (51)$$

and the corresponding mode form:

$$\psi_C(x) = e^{-j\gamma k_B x} + [r]_C e^{j\gamma k_B x} + [r_j]_C e^{\varepsilon k_B x} \quad (52)$$

where

$$[r]_C = \frac{-\varepsilon - j\gamma}{\varepsilon - j\gamma} \quad (53)$$

$$[r_j]_C = \frac{2j\gamma}{\varepsilon - j\gamma} \quad (54)$$

Let us first consider the case of a simply supported structural slab and a floating slab with free edges. We only have to consider the coupling between waves that have the same wavenumber along the edge, which means that:

$$k_{B_3} \sin\theta_3 = k_{B_1} \sin\theta_1 \quad (55)$$

If we assume a certain angle of incidence θ_3 in the structural slab we have

$$\theta_1 = \arcsin \left(\frac{k_{B_3}}{k_{B_1}} \sin\theta_3 \right) \quad (56)$$

Now, our usual situation is $k_{B_1} > k_{B_3}$, say that we have a 160 mm and a 50 mm concrete slab. Then we have from (56) that $0 < \theta_1 < 34^\circ$. Our previous assumption that the free wavefield in plate 1 is almost perpendicular to the edge seems to be reasonably correct. Our coupling strip w now varies with the angle of incidence, and we have:

$$w_{SF}(\theta) = \left| \int_0^{-\infty} e^{\delta x} \psi'_{S_3}(x, \theta) \psi'_{F_1}(x, \theta) dx \right| \quad (57)$$

After insertion of equations (44), (48) and considering (55) we get:

$$w_{SF}(\theta_3) = \left| \frac{1}{4} \int_0^{-\infty} e^{\delta x} \left[e^{-j\gamma_3 k_{B_3} x} - e^{j\gamma_3 k_{B_3} x} \right] \cdot \right. \\ \left. \left[e^{-j\gamma_1 k_{B_1} x} + [r]_{F_1} e^{j\gamma_1 k_{B_1} x} + [r_j]_{F_1} e^{\varepsilon_1 k_{B_1} x} \right] dx \right| \quad (58)$$

or

$$w_{SF}(\theta_3) = \frac{1}{2} \left| \int_0^{-\infty} e^{\delta x} \sin(\gamma_3 k_{B_3} x) \cos(\gamma_1 k_{B_1} x) [1 + r]_{F_1} dx + \int_0^{\infty} e^{\varepsilon_1 k_{B_1} x} \sin(\gamma_3 k_{B_3} x) [r_j]_{F_1} dx \right|$$

After evaluation of (59) we can write:

$$w_{SF}(\theta_3) = \frac{1}{k_{B_1}} \left| \frac{1}{(k_{B_3}/k_{B_1})^4 - 1} \right| w'_{SF}(\theta_3) \quad (60)$$

where

$$w'_{SF}(\theta_3) = \frac{1}{2} |r_j|_{F_1} \left| \gamma_3 \left(\frac{\beta_1}{\alpha_1} + 1 \right) \left(\frac{k_{B_3}}{k_{B_1}} \right)^3 + \gamma_3 \left(\frac{\beta_1}{\alpha_1} - 1 \right) \frac{k_{B_3}}{k_{B_1}} \right| \quad (61)$$

Finally we integrate over all angles of incidence in the structural slab and normalize. We assume a diffuse field over the area, which means that all angles of incidence are equally probable:

$$\bar{w} = \frac{2}{\pi} \int_0^{\pi/2} [w(\theta_3)]^2 d\theta_3 \quad (62)$$

Our equation for w_{SF} then becomes:

$$\bar{w}_{SF}^2 = \frac{1}{k_{B_1}^2} \left| \frac{1}{(k_{B_3}/k_{B_1})^4 - 1} \right|^2 I_{SF} \quad (63)$$

where I_{SF} , which is different for different constructions, can be calculated from the equation:

$$I_{SF} = \frac{2}{\pi} \int_0^{\pi/2} [w'_{SF}(\theta_3)]^3 d\theta_3 \quad (64)$$

This has been done numerically for four different plate configurations, and the results are shown below. The Poisson's ratio $\mu = 0.25$ has been used in the calculations, but the results are not very sensitive to changes in μ .

Table 3.3.3.1 (Average over θ_3).

Structural slab: SSSS

Floating slab: FFFF

Isf: Diffuse incidence

Isfo: Normal incidence

Fc3/Fc1	Isf	Isfo
120/1200	.0010	.0020
180/1200	.0034	.0068
120/ 380	.0324	.0630
180/ 380	.1092	.2126

To show the variation of coupling with different angles of incidence, we have plotted the function $w'_{SF}(\theta_3)$ from equation (61) for two of these configurations. The two represent the cases of the most similar and the most dissimilar plates respectively

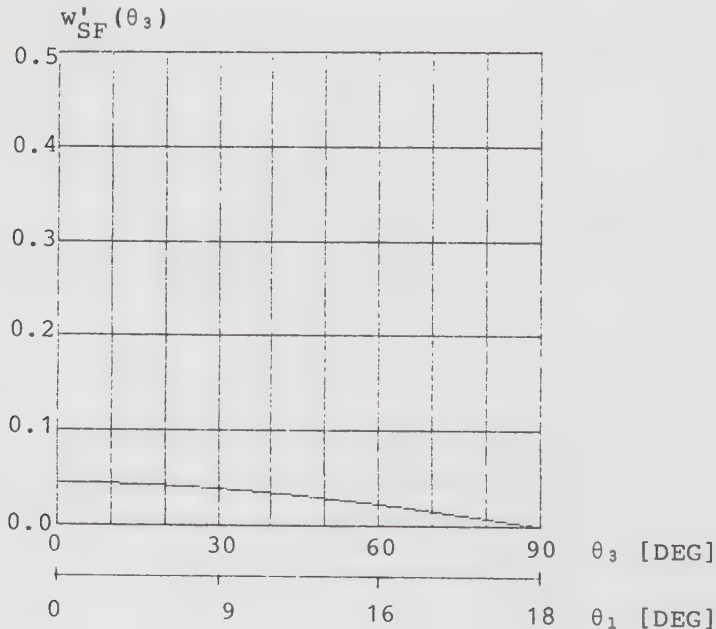


Figure 3.3.3.2. 22 mm chip board, 160 mm concrete slab
 $(f_{c1}/f_{c3} = 1200/120)$.

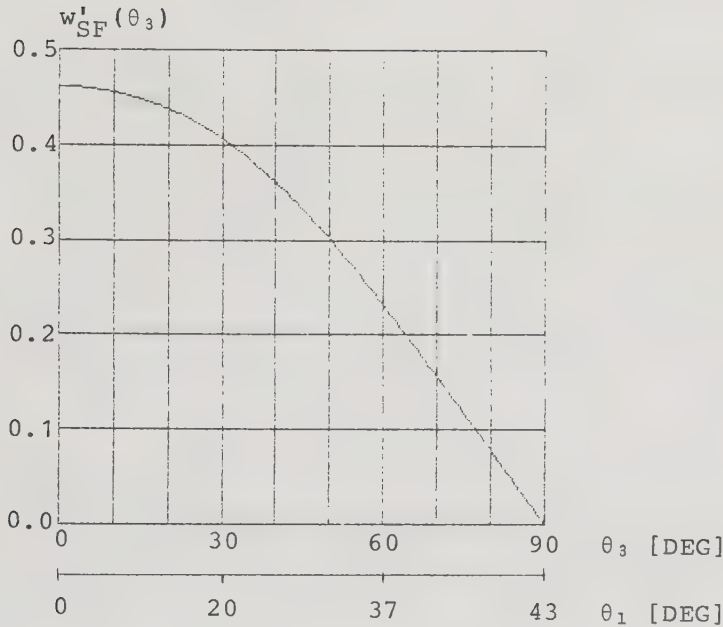


Figure 3.3.3.2. 50 mm concrete slab, 200 lightweight concrete ($f_{C_1}/f_{C_3} = 380/180$).

We see that the relationship between normal and diffuse incidence is almost the same in all our four examples above (see Table 3.3.3.1). Indeed if we take a closer look at equation (61) we see that the only variable that varies fast with the angle of incidence is γ_3 . A fairly good approximation might be to set $|r_j|_{F_1} \approx \sqrt{2}$ and $\beta_1/\alpha_1 \approx 1$ as for normal incidence and only integrate over γ_3 . This would give $\bar{w}_{SF}^2 = \frac{2}{\pi} [w_{SF}(0^0)]^2$, but this is slightly too high according to Table 3.3.3.1. Instead of $2/\pi$ the numerical factor is very close to 0.5, so it seems that we can write

$$\bar{w}_{SF}^2 \approx \frac{1}{k_{B_1}^2} \left| \frac{1}{(k_{B_3}/k_{B_1})^4 - 1} \right|^2 \left(\frac{k_{B_3}}{k_{B_1}} \right)^6 \quad (65)$$

This approximation has been shown to be valid in comparison to (63) even for plates that are almost identical.

However, our calculations are really made in terms of plate 1, and when we continue and calculate Y_{SF} as for the 1-dimensional model, we should have w averaged over θ_1 . Only a sector of θ_1 is active in the coupling, and we might correct (65) by a factor that takes this into account. The limiting angle is

$$\theta_{1, \text{lim}} = \arcsin(k_{B_3}/k_{B_1}) \approx k_{B_3}/k_{B_1} \quad (66)$$

where the approximation is valid for most configurations in building acoustics. The correction factor would then be $(2/\pi) \cdot k_{B_3}/k_{B_1}$ giving:

$$\bar{w}_{SF}^2 \approx \frac{2}{\pi k_{B_1}^2} \left| \frac{1}{(k_{B_3}/k_{B_1})^4 - 1} \right|^2 \left(\frac{k_{B_3}}{k_{B_1}} \right)^7 \quad (67)$$

A better way to get w_{SF} averaged over θ_1 is of course to make the integration in equation (64) over θ_1 instead:

$$I_{SF} = \frac{1}{\theta_{1, \text{lim}}} \int_0^{\theta_{1, \text{lim}}} [w'_{SF}(\theta_1)]^2 d\theta_1 \quad (68)$$

Table 3.3.3.2 shows the results of (68) for the same four configurations:

Table 3.3.3.2 (Average over θ_1).

Structural slab: SSSS
 Floating slab: FFFF
 Isf: Diffuse incidence!
 Isfo: Normal incidence

F_{c3}/F_{c1}	Isf	Isfo
120/1200	.0014	.0020
180/1200	.0047	.0068
120/ 380	.0428	.0630
180/ 380	.1418	.2126

As before we show the variation of coupling of different angles of incidence by plotting $w'_{SF}(\theta_1)$ from equation (61). The two same examples as before are chosen.

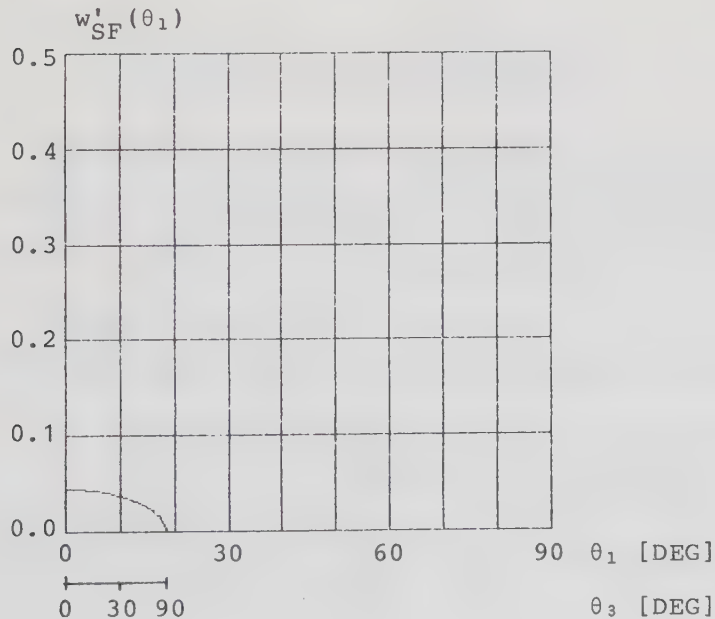


Figure 3.3.3.4. 22 mm chip board, 160 mm concrete slab ($f_{c1}/f_{c3} = 1200/120$).

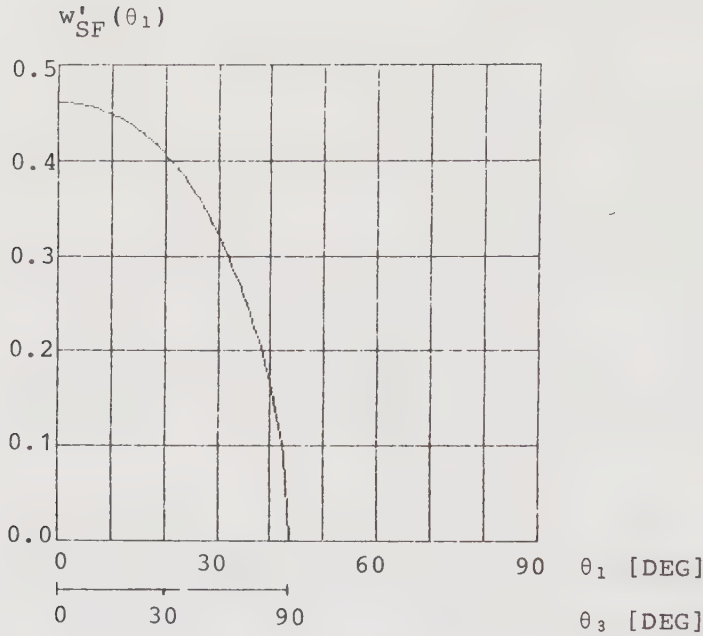


Figure 3.3.3.5. 50 mm concrete slab, 200 lightweight concrete ($f_{c1}/f_{c3} = 380/180$).

Now proceeding in a similar way as before we find that the diffuse value of I_{SF} is coupled to the value for normal incidence I_{SF0} as $I_{SF} \approx (2/3)I_{SF0}$. This results in a new equation for $\bar{w}_{SF}^2$:

$$\bar{w}_{SF}^2(0 \rightarrow \theta_{lim,1}) \approx \frac{4}{3k_{B1}^2} \left| \frac{1}{(k_{B3}/k_{B1})^4 - 1} \right|^2 \left(\frac{k_{B3}}{k_{B1}} \right)^6 \quad (69)$$

or when averaged over all angles (even those $\theta_1 > \theta_{1,lim}$, which give no coupling):

$$\bar{w}_{SF}^2(0 \rightarrow \pi/2) \approx \frac{8}{3\pi k_{B1}^2} \left| \frac{1}{(k_{B3}/k_{B1})^4 - 1} \right|^2 \left(\frac{k_{B3}}{k_{B1}} \right)^7 \quad (70)$$

The coupling, C , can here be estimated as:

$$C_{SF} = \frac{\bar{w}_{SF}^U}{S} \quad (71)$$

Inserting this into equation (12) as before, we may use the mode density of equation (14) together with $\bar{w}_{SF}$ of equation (70). We may also take $\bar{w}_{SF}$ of equation (69), but then we have only coupling to the modes within a certain sector, which means that we have to limit the mode density of equation (14):

$$\begin{aligned} \left(\frac{\Delta n}{\Delta f} \right)_{0 \rightarrow \theta_{1, \text{lim}}} &= \left(\frac{k_B^2 S}{4\pi f} \right) \cdot \left(\frac{\theta_{1, \text{lim}}}{\pi/2} \right) \\ &\approx \left(\frac{k_B^2 S}{4\pi f} \right) \cdot \left(\frac{2k_{B_3}}{\pi k_{B_1}} \right) \end{aligned} \quad (72)$$

Both ways of thinking result in the same equation:

$$\begin{aligned} Y_{SF} &= \frac{\langle v_{1, \text{res}}^2 \rangle}{\langle v_{1, \text{forced}}^2 \rangle} = \frac{1}{\eta^2} \frac{8U}{3\pi k_{B_1}^2 S^2} \left(\frac{k_{B_3}}{k_{B_1}} \right)^7 \left(\frac{k_{B_1}^2 S \eta}{8} \right) \\ &= \frac{U^2}{3\pi S \eta} \left(\frac{k_{B_3}}{k_{B_1}} \right)^7 \end{aligned} \quad (73)$$

An interesting detail is that different modes may in general be excited at different edges. If we assume the plate to be rectangular with x-edges at $z = (0, a)$ and z-edges at $x = (0, b)$, then the most oblique modes at the x-edges are the most normal modes at the z-edges and vice versa.

Let us now consider the case of a clamped structural slab, and a floating slab with free edges. Here we get:

$$w_{CF}(\theta) = \left| \int_0^{-\infty} e^{\delta x} \psi'_{C_3}(x, \theta) \psi'_{F_1}(x, \theta) dx \right| \quad (74)$$

After insertion of equations (48) and (52) we have:

$$w_{CF}(\theta_3) = \left| \frac{1}{4} \int_0^{-\infty} e^{\delta x} \left[e^{-j\gamma_3 k_{B_3} x} + [r]_{C_3} e^{j\gamma_3 k_{B_3} x} + [r_j]_{C_3} e^{\varepsilon_3 k_{B_3} x} \right] \cdot \right. \\ \left. \left[e^{-j\gamma_1 k_{B_1} x} + [r]_{F_1} e^{j\gamma_1 k_{B_1} x} + [r_j]_{F_1} e^{\varepsilon_1 k_{B_1} x} \right] dx \right| \quad (75)$$

The evaluation is rather more tedious here, but we finally get:

$$w_{CF}(\theta_3) = \frac{1}{k_{B_1}} \frac{1}{(k_{B_3}/k_{B_1})^4 - 1} w'_{CF}(\theta_3) \quad (76)$$

where $w'_{CF}(\theta_3)$ can be written as follows, where we have introduced $\kappa = (k_{B_3}/k_{B_1})$:

$$w'_{CF}(\theta_3) = \frac{1}{4} \left| j\gamma_3 (\kappa^3 + \kappa) [1+r]_{F_1} [-1+r]_{C_3} - j\gamma_1 (\kappa^2 + 1) [1+r]_{C_3} [-1+r]_{F_1} \right. \\ + j\gamma_3 (\kappa^3 - \kappa) [r_j]_{F_1} [-1+r]_{C_3} - \varepsilon_1 (\kappa^2 - 1) [r_j]_{F_1} [1+r]_{C_3} \\ + j\gamma_1 (\kappa^2 - 1) [r_j]_{C_3} [-1+r]_{F_1} - \varepsilon_3 (\kappa^3 - \kappa) [r_j]_{C_3} [1+r]_{F_1} \\ \left. - [r_j]_{C_3} [r_j]_{F_1} (\varepsilon_3 (\kappa^3 + \kappa) - \varepsilon_1 (\kappa^2 + 1)) \right| \quad (77)$$

As for the simply supported structural slab, we integrate over all angles of incidence and get:

$$\bar{w}_{CF}^2 = \left| \frac{1}{(k_{B_3}/k_{B_1})^4 - 1} \right|^2 I_{CF} \quad (78)$$

When averaged over the angles θ_1 up to the limiting angle we get:

$$I_{CF} = \frac{1}{\theta_{1, \lim}} \int_0^{\theta_{1, \lim}} [w'_{CF}(\theta_1)]^2 d\theta_1 \quad (79)$$

The coupling here shows somewhat more interesting variations with different angles of incidence than our previous case. Below we show the function $w'_{CF}(\theta_3)$ from equation (77) for our four configurations in the order of increasing similarity between the plates.

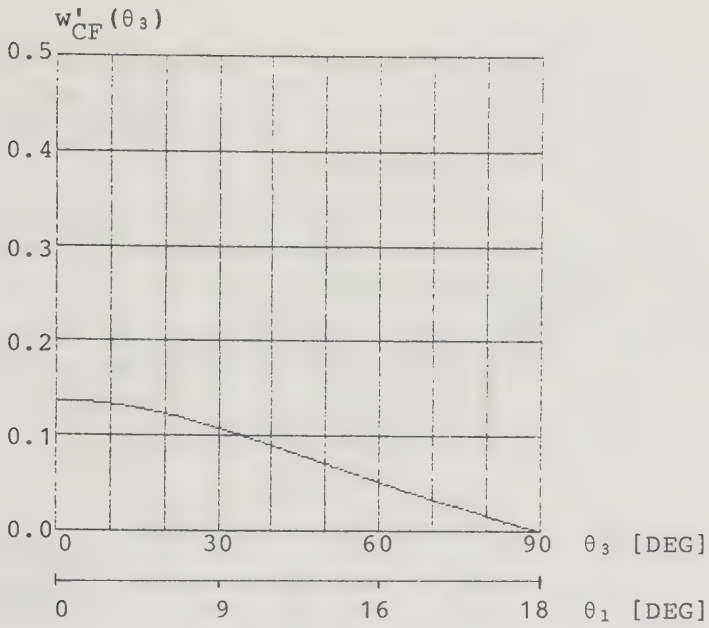


Figure 3.3.3.6. 22 mm chip board, 160 mm concrete slab ($f_{C1}/f_{C3} = 1200/120$).

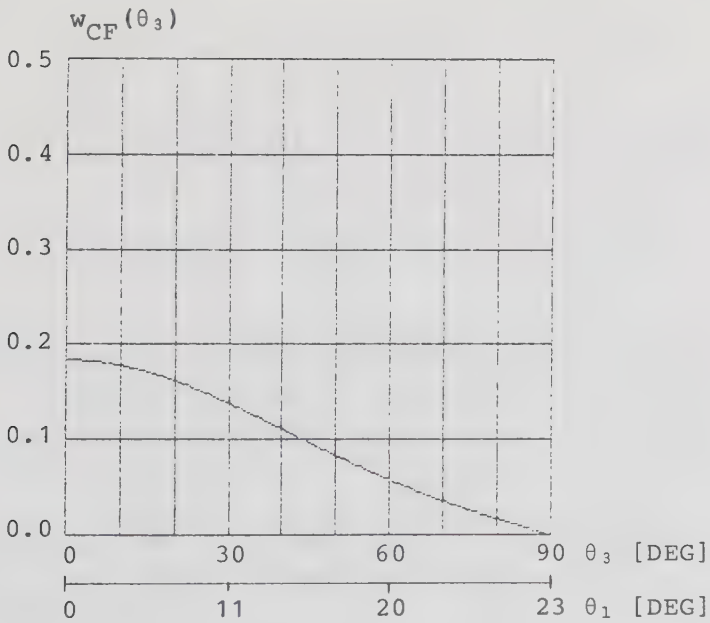


Figure 3.3.3.7. 22 mm chip board, 160 mm lightweight concrete ($f_{C1}/f_{C3} = 1200/180$).

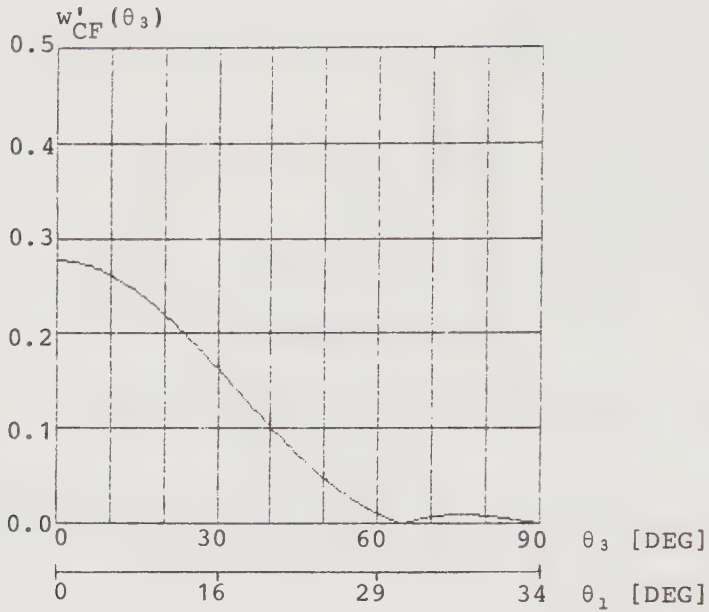


Figure 3.3.3.8. 50 mm concrete slab, 160 mm concrete slab ($f_{C1}/f_{C3} = 380/120$).

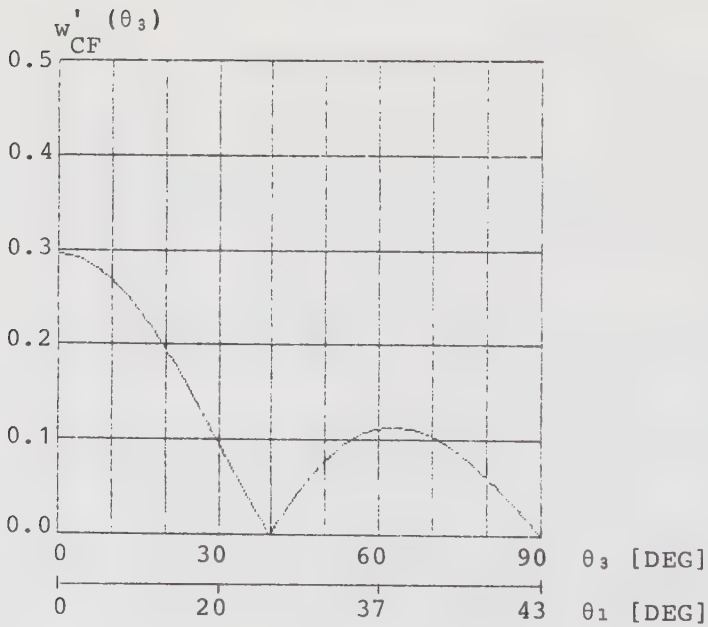


Figure 3.3.3.9. 50 mm concrete slab, 200 mm light-weight concrete ($f_{C1}/f_{C3} = 380/180$).

We see that when the plates become sufficiently similar, we have an angle of incidence (other than $\theta_3 = \pi/2$) where there is no coupling.

If we make the plates even more similar, we may have a stronger coupling for some sector of angles than the coupling at normal incidence. The diffuse coupling may then be stronger than the one for normal incidence. Below is a fictitious example, where we have chosen $f_{c_1}/f_{c_3} = 380/280$ to demonstrate this.

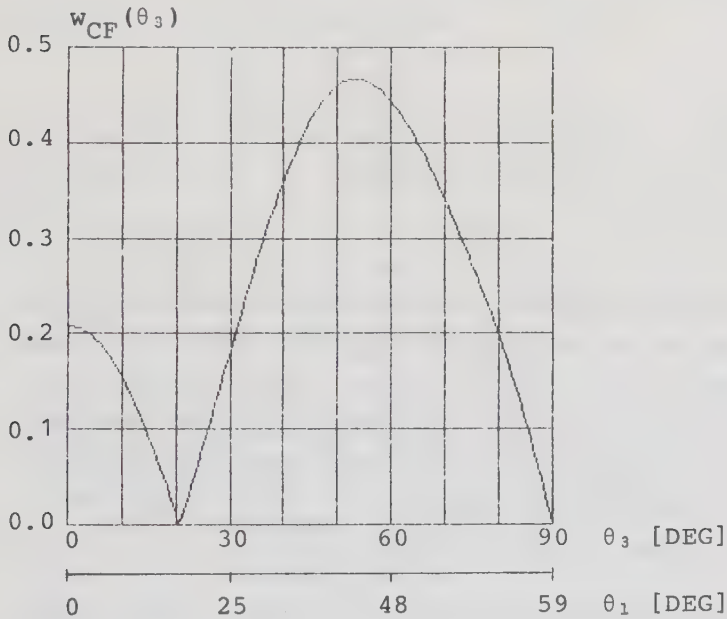


Figure 3.3.3.10. Fictitious example
 $(f_{c_1}/f_{c_3} = 380/280)$.

In Table 3.3.3.3 we show the values of I_{CF} integrated over θ_1 according to equation (79) for our four practical constructions:

Table 3.3.3.3 (Average over θ_1).

Structural slab: CCCC
Floating slab: FFFF

Icf: Diffuse incidence
Icfo: Normal incidence

Fc3/Fc1	Icf	Icfo
120/1200	.0112	.0187
180/1200	.0190	.0338
120/ 380	.0318	.0765
180/ 380	.0293	.0872

Here we find no simple relationship between I_{CF} and I_{CF_0} , but we can state that as for normal incidence the clamped case gives stronger coupling than the simply supported case, when the plates are very dissimilar. The reverse is true when the plates are similar, such as our case when $f_{C_1}/f_{C_3} = 380/180$. (Compare Tables 3.3.3.2 and 3.3.3.3.)

If the plates become even more similar, such as our previous case when $f_{C_1}/f_{C_3} = 380/280$, we find that I_{CF_0} becomes very much smaller than I_{SF_0} . The diffuse values however do not differ as much, because of the extra "bump" at oblique incidence for the clamped case.

Now, many practical floating floors with a heavy floating slab lie in the interval $2.5 < f_{C_1}/f_{C_3} < 5$. In this interval the quotient I_{SF}/I_{CF} varies

approximately between: $2.5 < I_{SF}/I_{CF} < 0.5$. We also know that the boundary conditions of the structural slab in most cases lie somewhere between clamped and simply supported. Taking all this into account it may be considered an acceptable estimation for floating floors with a heavy floating slab in general to use the model of a simply supported structural slab. However if the boundary conditions are known to be almost clamped and the configuration has f_{C_1}/f_{C_3} outside the interval $2.5 < f_{C_1}/f_{C_3} < 5$, an evaluation by using the integral (79) would be appropriate. The equation for Y_{CF} would then be

$$Y_{CF} = \frac{I_{CF} U^2}{4\pi\eta_1 S} \left(\frac{k_{B_3}}{k_{B_1}} \right) \quad (80)$$

For the case of a light floating plate and a heavy structural slab (e.g. chip board, concrete slab) the resonant coupling may be disregarded for a simply supported structural slab in most cases. For a clamped structural slab however, it may have to be considered.

Below is a table that shows a comparison between some ratio factors Y_{SF} , Y_{CF} for three of our four configurations, evaluated for different calculation models.

Table 3.3.3.4. Comparison between different ratio factors $Y = \langle v_{1,res}^2 \rangle / \langle v_{1,forced}^2 \rangle$.

Model	Expression	$\frac{f_{c1}}{f_{c3}} = \frac{380}{180}$	$\frac{f_{c1}}{f_{c3}} = \frac{380}{120}$	$\frac{f_{c1}}{f_{c3}} = \frac{1200}{120}$
Lindblad's approximate formula (see equation (3.2.19))	$Y = \frac{\lambda_{B1} U}{\pi^2 \eta_1 S}$	$\frac{0.21}{\eta_1} \sqrt{\frac{100}{f}}$	$\frac{0.21}{\eta_1} \sqrt{\frac{100}{f}}$	$\frac{0.12}{\eta_1} \sqrt{\frac{100}{f}}$
One dim. model of 3.3.2 (equations (38), (39))	$Y_{SF} = \frac{2U}{k_{B1} \eta_1 S} \left(\frac{k_{B3}}{k_{B1}} \right)^6$	$\frac{0.07}{\eta_1} \sqrt{\frac{100}{f}}$	$\frac{0.02}{\eta_1} \sqrt{\frac{100}{f}}$	$\frac{0.0004}{\eta_1} \sqrt{\frac{100}{f}}$
	$Y_{CF} = \frac{4U}{k_{B1} \eta_1 S} \left(\frac{k_{B3}}{k_{B1}} \right)^4 \left(\frac{k_{B3}}{k_{B1}} - 1 \right)^2$	$\frac{0.03}{\eta_1} \sqrt{\frac{100}{f}}$	$\frac{0.02}{\eta_1} \sqrt{\frac{100}{f}}$	$\frac{0.003}{\eta_1} \sqrt{\frac{100}{f}}$
Oblique inc. model of 3.3.3 (equations (73), (80))	$Y_{SF} = \frac{U^2}{3\pi \eta_1 S} \left(\frac{k_{B3}}{k_{B1}} \right)^7$ $Y_{CF} = \frac{I_{CF} U^2}{4\pi \eta_1 S} \left(\frac{k_{B3}}{k_{B1}} \right)$	$\frac{0.13}{\eta_1}$ $\frac{0.03}{\eta_1}$	$\frac{0.03}{\eta_1}$ $\frac{0.02}{\eta_1}$	$\frac{0.0005}{\eta_1}$ $\frac{0.005}{\eta_1}$

The construction elements in the table are a 160 mm concrete slab ($f_{c3} = 120$ Hz), 200 mm lightweight concrete ($f_{c3} = 180$ Hz), 50 mm concrete slab ($f_{c1} = 380$ Hz) and 22 mm chip board ($f_c = 1200$ Hz). U/S has been chosen as 14 m/12 m².

3.4 Radiation efficiencies and corrected bending waves

3.4.1 Radiation efficiencies

We have now deduced expressions for the ratio of resonant vibrations to forced vibrations, Y . We have already in part 3.2 pointed out that the correction term because of resonant transmission should be written as:

$$\Delta\Delta L_{\text{res}} = -10 \log \left(1 + Y \frac{\sigma_{1,\text{res}}}{\sigma_{\text{forced}}} \right) \quad (1)$$

σ_{forced} is the same radiation efficiency as the structural slab has by itself. σ_{res} on the other hand is the radiation efficiency of the resonant field in the floating slab. We have already pointed out that not all the modes are activated, but primarily those which are almost perpendicular to the edges. Below f_{c_1} we have mainly edge-radiation from the plate, and only those modes can radiate that have their trace-velocities along the edges larger than c . To have large trace-velocities the modes must be almost perpendicular to the edge, and thus almost all excited modes in the floating plate can radiate at some edge.

For this situation we have the following radiation efficiency for a simply supported and baffled plate (see e.g. [21]):

$$\sigma' = \frac{\lambda_c}{4\pi} \frac{U}{S} \quad (2)$$

For a clamped plate σ' is increased by a factor 2 and if there are walls around the edges, σ' is again increased by a factor 2.

However we have neither a simply supported nor a clamped floating slab. It can be considered to have free edges in most cases. A plate with free edges is treated in [28], and the result is that such a plate couples very poorly to airborne sound for $f \ll f_c$. The radiation efficiency is very much lower than that for a simply supported plate in equation (2). Above f_c the radiation efficiency can be approximated by $\sigma' \approx 1$.

We have thus:

$$\sigma_{1,res} \approx 0 \quad f < f_c \quad (3)$$

$$\sigma_{1,res} \approx 1 \quad f > f_c \quad (4)$$

Above f_{c1} , we can also assume σ_{forced} to be close to one. Thus the result is that the resonant transmission should be negligible at frequencies below f_{c1} but above f_{c1} it can be taken into account by using the correction term:

$$\Delta\Delta L_{res} = -10 \log (1 + Y) \quad (5)$$

3.4.2 Corrected bending waves

As a final remark it is appropriate here to point out that throughout this chapter it has been assumed without comments, that the ratio k_{B1}/k_{B3} is equal to $\sqrt{f_{c1}/f_{c3}}$. This is a good approximation in the frequency region well above the resonance frequency f_1 and up to the frequency where $\lambda_{B1} = 6 h_1$. Above this upper frequency limit, which marks the end of the region of applicability for the simple bending wave equation, the phase velocity of transverse waves, asymptotically approaches the phase velocity of Rayleigh waves, c_R .

Thus instead of increasing as $k_B \sim \sqrt{f}$, the wavenumbers approach the increase $k_B \sim f$ at higher frequencies. This can be calculated properly by using the corrected bending wave theory of Mindlin [31].

A reasonably accurate approximation equation may however be sufficient in many cases:

$$c'_B \approx (c_B^{-n} + c_R^{-n})^{-1/n} \quad (6)$$

or considering $c_R = 0.9554 c_T$, where c_T is the velocity of "shear waves":

$$c_T = \sqrt{G/\rho} \quad (7)$$

we may write, choosing $n = 4$:

$$\begin{aligned} c'_B &\approx (c_B^{-4} + c_T^{-4})^{-1/4} \\ &= \left(\frac{m}{\omega^2 B} + \left(\frac{2(1+\mu)}{E/\rho} \right)^2 \right)^{-1/4} \\ &= \left(\frac{(f_c/f)^2}{c^4} + \frac{4(1+\mu)^2}{c_L^4} \right)^{-1/4} \end{aligned} \quad (8)$$

This type of "correlated bending wave" has been suggested by Lindblad in [21]. Below we have drawn c'_B together with c_B and the exact solution, which is c_B + rotatory inertia correction + shear correction. The exact curve is borrowed from [31], and there it is shown that the rotatory inertia correction is almost negligible. The shear correction accounts for almost the entire correction.

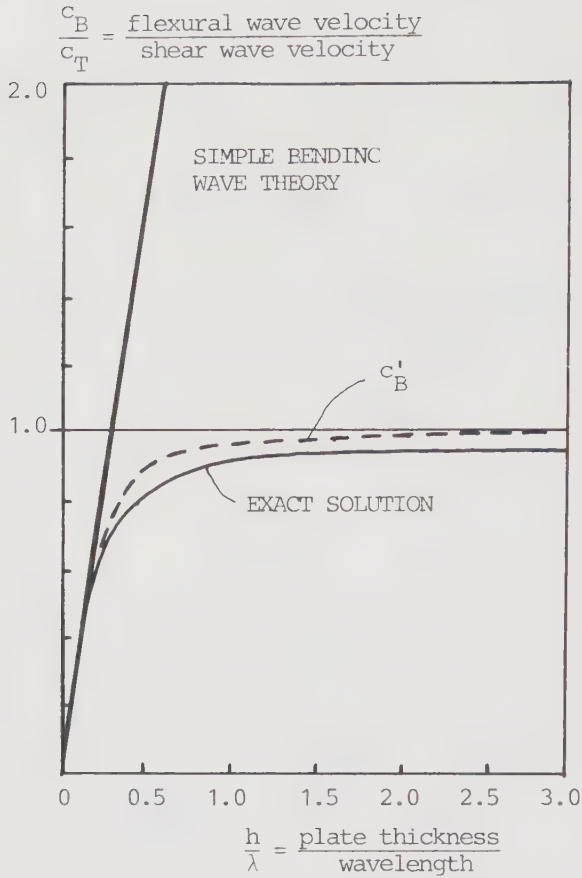


Figure 3.4.2.1. Phase velocity of transverse waves in a plate. Exact solution from [31].

It is evident that the simply corrected c_B^1 from equation (8) gives a sufficiently accurate approximation for many types of calculations.

Our 200 mm slab of lightweight concrete has $\lambda_B = 6 h$ already at $f = 450$ Hz, and the 160 mm concrete slab at $f = 1050$ Hz. Above these respective frequencies the corrected bending wave speed c_B^1 should be used.

For the thinner floating plates from our examples, the simple bending wave theory is applicable throughout the frequency region of building acoustics.

If we calculate (k_{B_1}/k'_{B_3}) for the lightweight structural slab and the 50 mm concrete slab at different frequencies we find:

f (Hz)	k_{B_1}/k'_{B_3}
500	$1.45 = \sqrt{f_{C_1}/f_{C_3}}$
1000	1.33
2000	1.14

This would mean a noticable increase in the coupling at higher frequencies: $(1.45/1.14)^2 \approx 5$, implying a 5 times stronger resonant field at 2000 Hz than at 500 Hz for this configuration.

4. THE INTERLAYER AS A WAVE MEDIUM

4.1 Historical review

It is of course a well known fact, that springs can only be considered as lumped, massless elements at relatively low frequencies. At higher frequencies the spring has to be treated as a medium with distributed mass per unit of length and a specific stiffness per unit of length. This leads to a differential equation, which produces solutions of the type $e^{j\omega t + jk_s x}$, i.e. longitudinal waves with wavenumber k_s and phase velocity $c_s = \omega/k_s$.

The same is true for our interlayer, which we continue to consider as locally reacting.

However, studying the literature for floating floors it seems as though this fact has not been taken into account until quite recently.

In 1982 Lindblad [32] treats the elastic interlayer as a wave medium with wave impedance $Z_s = \sqrt{D_s \rho_s}$, velocity $c_s = \sqrt{D_s / \rho_s}$ and wavenumber $k_s = \omega / c_s$. Here D_s and ρ_s are dynamic modulus and density of the interlayer respectively. Lindblad then states that this implies for the primary wave:

$$\frac{\tau_{\text{total construction}}}{\tau_{\text{structural slab}}} = \left| \frac{2 \cdot Z_s}{j\omega m_1} \right|^2 \quad (1)$$

or

$$\begin{aligned} \Delta L_1 &= 20 \log \left(\frac{\omega m_1}{2 \rho_s c_s} \right) \\ &= R_0 - 20 \log \left(\frac{\rho_s c_s}{\rho c} \right) \end{aligned} \quad (2)$$

Here R_0 is the transmission loss factor for the floating slab at normal incidence.

The losses in the interlayer are then accounted for with:

$$\Delta L_{\eta} = -10 \log e^{-k_s \eta_2 \cdot h_2} \quad (3)$$

Finally the multiple reflections are estimated by considering the interlayer as a 1-dimensional space. giving through power balance the following correction term:

$$\Delta L_r = 10 \log \left(1 + \frac{1}{2k_s \eta_2 h_2} \right) \quad (4)$$

This transmission is considered as a complement to Cremer's forced solution, which should be valid at low frequencies. At higher frequencies ΔL would be given by the combination of equations (2), (3), (4):

$$\Delta L = R_0 - 10 \log \left(\frac{\rho_s^c}{\rho c} \right) + \Delta L_{\eta} - \Delta L_r \quad (5)$$

(Yet another correction term is added for the resonant transmission, but this has already been treated in chapter 3.)

In 1983 Lindblad [22] develops his theory further. Instead of starting with Cremer's theory at low frequencies, he now starts with (2) and (3), and then treats the reflections which lead to resonances including the fundamental one. The repetitive reflections produce a geometrical series with terms of the kind G^n , where $G = \Gamma_1 \Gamma_3 e^{-jk_s 2h_2}$.

$$|G| = |\Gamma_1| |\Gamma_3| \cdot e^{-k_s \eta_2 h_2} \quad (6)$$

$$\arg G = \varphi = \gamma_1 + \gamma_3 - k_s \cdot 2h_2 \quad (7)$$

Here Γ is the complex reflection factor for sound pressure at the respective slabs:

$$\Gamma = \frac{j\omega m - Z_s}{j\omega m + Z_s} \quad (8)$$

and

$$\Gamma = |\Gamma| \cdot e^{j\gamma} \quad (9)$$

The geometrical series has an analytic solution:

$\sum_0^\infty G^n = (1 - G)^{-1}$ and the correction factor for the reflections is:

$$\Delta L^{\text{refl}} = 20 \log |1 - G| \quad (10)$$

or

$$\Delta L^{\text{refl}} = 10 \log ((1 - |G| \cos \varphi)^2 + (|G| \sin \varphi)^2) \quad (11)$$

Now to find the values φ and $|G|$ we must first evaluate γ and $|\Gamma|$

$$|\Gamma|^2 = \left(\frac{(\omega m)^2 - Z_s^2}{(\omega m)^2 + Z_s^2} \right)^2 + \left(\frac{2\omega m Z_s}{(\omega m)^2 + Z_s^2} \right)^2 \quad (12)$$

$$\gamma = \arctan \frac{2\omega m Z_s}{(\omega m)^2 - Z_s^2} \quad (13)$$

If now $\omega m \gg Z_s$ (heavy slabs or relatively high frequency) (12) and (13) can be approximated as:

$$|\Gamma| \approx 1 \quad (14)$$

$$\gamma \approx \frac{2Z_s}{\omega m} \quad (15)$$

This gives the following expression for $|G|$ and φ :

$$|G| \approx e^{-k_s h_2 n_2} \quad (16)$$

$$\varphi \approx \frac{2Z_s}{\omega m_1} + \frac{2Z_s}{\omega m_3} - 2h_2 k_s \quad (17)$$

At the resonances we have

$$\Delta L^{\text{refl}} = 20 \log (1 - |G|) \quad (18)$$

and this occurs at $\varphi = -2n\pi$.

The fundamental resonance is then ($n = 0$):

$$f_{13} = \frac{1}{2\pi} \sqrt{\frac{Z_s c_s (m_1 + m_3)}{h_2 m_1 m_3}} \quad (19)$$

Considering $Z_s = \rho_s c_s$ and $D_s = \rho_s c_s^2$, we can also write

$$f_{13} = \frac{c_s}{2\pi} \sqrt{\frac{\rho_s (m_1 + m_3)}{h_2 m_1 m_3}} = \frac{1}{2\pi} \sqrt{\frac{D_s (m_1 + m_3)}{h_2 m_1 m_3}} \quad (20)$$

For the higher order resonances, the two first terms of (17) can be disregarded in most cases, and

$$f_n = \frac{nc_s}{2h_2} \quad (21)$$

Typical values for a dense mineral wool are $D_s \approx 4 \cdot 10^5 \text{ Pa}$ and $\rho_s \approx 150 \text{ kg/m}^3$, giving $c_s \approx 50 \text{ m/s}$. For a typical width of interlayer, $h_2 \approx 0.05 \text{ m}$, this means $f_n \approx n \cdot 500 \text{ Hz}$.

Now the total sound insulation improvement can be written as:

$$\Delta L = \Delta L_1 + \Delta L_n + \Delta L^{\text{refl}} \quad (22)$$

where ΔL^{refl} also includes Cremer's solution.

(There is also here a correction factor for the resonant transmission, but this has already been treated in chapter 3.)

A second example of calculations, where the interlayer is treated as a wave medium is the work of Widén [23] 1982. He points out that the insulation improvement should be drastically reduced at the frequencies f_n of equation (21). He also expects (erroneously) anti-resonances to occur at frequencies $f_m = m \cdot f_n / 2$, but finds no such phenomenon in his measurements. This he explains with the anisotropic nature of the mineral wool. Widén unfortunately publishes no formulas, but he shows some calculation examples, where his curves have a similar form to the ones given by Lindblad's equations. Widén finds good agreement between his calculations and the measurements of Nilsson [10] and reasonably good agreement with his own model measurements.

Other examples of this method of attack have not been found in literature. However it can be mentioned that in [33] Kovrigin refers to a Russian method to calculate impact sound insulation for floating floors. ("Instructions for calculation of impact sound insulation for floating floors and floors with coverings (in Russian), Chelyabinsk, 1965".)

The method follows Cremer's forced solution at low frequencies. This is however corrected slightly if the slabs are very similar or very dissimilar. The corrected formula is:

$$\Delta L = 10 \log \left[(f/f_1)^2 \left((f/f_1)^2 - 2 \frac{m_3 - m_1}{m_3} + \left(\frac{m_3 + m_1}{m_3} \right)^2 \right) \right] \quad (23)$$

At middle and high frequencies the following formula is used:

$$\Delta L = 20 \log (f/f_1) + 10 \log \left(\frac{m_1}{m_2} \right) - 3 \text{ dB} \quad (24)$$

This formula can be rewritten, considering

$$\omega_1^2 = (\rho_s c_s^2) / (h_2 m_1) \text{ and } m_2 = \rho_s h_2:$$

$$\begin{aligned} \Delta L &= 20 \log \left(\frac{\omega m_1}{\rho_s c_s} \right) - 3 \text{ dB} \\ &= R_0 - 20 \log \left(\frac{\rho_s c_s}{\rho c} \right) - 3 \text{ dB} \end{aligned} \quad (25)$$

This is the same equation as Lindblad's (5), if we set $\Delta L_\eta - \Delta L_r = -3 \text{ dB}$.

No explanations are given in [33] as to whether the equations (23), (24) are analytical or empirical. The original literature has not been studied.

4.2 Extension of the Fourier transform method

We would now like to extend our calculation method based on Fourier transforms (see part 2.3) to include the wave behaviour of the interlayer.

A suitable method to solve this problem is to use four-pole parameter analysis (see e.g. [34] or [35]). We choose the type of four-pole analysis that describes the input velocity and pressure in terms of the output velocity and pressure.

In general we have a linear mechanical system as shown schematically in the figure below



Figure 4.2.1. General four-pole mechanical system.

The input side is excited with the pressure p_1 , which results in the input velocity v_1 . The output side has the velocity v_2 and exerts the pressure p_2 on the next system.

In general we can write:

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} \alpha_{11} & \alpha_{12} \\ \alpha_{21} & \alpha_{22} \end{bmatrix} \begin{bmatrix} p_2 \\ v_2 \end{bmatrix} \quad (1)$$

The four-pole parameters α_{ij} are in general frequency dependent complex quantities.

The parameters can be found by setting $p_2 = 0$ or $v_2 = 0$.

In the case of a plate as the mechanical system we have:

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} 1 & (Z_p + Z_f) \\ 0 & 1 \end{bmatrix} \begin{bmatrix} p_2 \\ v_2 \end{bmatrix} \quad (2)$$

where Z_p is the impedance of the plate and Z_f is the radiation loading. This gives us:

$$\frac{p_1}{v_2} = \frac{p_2}{v_2} + (Z_p + Z_f) = Z_p + 2Z_f \quad (3)$$

This is a relationship we used in part 2.3.2.

When we have a spring as the mechanical system the four-pole equation is:

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} 1 & 0 \\ j\omega/K & 1 \end{bmatrix} \begin{bmatrix} p_2 \\ v_2 \end{bmatrix} \quad (4)$$

Now if we couple two plates with a spring (locally reacting interlayer) we may write:

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} 1 & (Z_{p_1} + Z_f) \\ 0 & 1 \end{bmatrix} \begin{bmatrix} 1 & 0 \\ j\omega/K & 1 \end{bmatrix} \begin{bmatrix} 1 & Z_{p_3} \\ 0 & 1 \end{bmatrix} \begin{bmatrix} p_3 \\ v_3 \end{bmatrix} \quad (5)$$

or

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} 1 + j\omega(Z_{p_1} + Z_f)/K & Z_{p_3} + (Z_{p_1} + Z_f)(1 + j\omega Z_{p_3}/K) \\ j\omega/K & 1 + j\omega Z_{p_3}/K \end{bmatrix} \begin{bmatrix} p_3 \\ v_3 \end{bmatrix} \quad (6)$$

This gives us:

$$\frac{p_1}{v_3} = (Z_{p_1} + Z_f) + (Z_{p_3} + Z_f) + \frac{(Z_{p_1} + Z_f)(Z_{p_3} + Z_f)}{K/j\omega} \quad (7)$$

This too is a relationship we used in part 2.3.2.

The four-pole equation for a spring element with distributed mass ρ_s , wavenumber k_s and phase velocity c_s , can be written as:

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} \cos(k_s h_2) & j\rho_s c_s \sin(k_s h_2) \\ j\sin(k_s h_2)/(\rho_s c_s) & \cos(k_s h_2) \end{bmatrix} \begin{bmatrix} p_2 \\ v_2 \end{bmatrix} \quad (8)$$

When the two plates are coupled together with this element the result is:

$$\begin{bmatrix} p_1 \\ v_1 \end{bmatrix} = \begin{bmatrix} \alpha_{11} & \alpha_{12} \\ \alpha_{21} & \alpha_{22} \end{bmatrix} \begin{bmatrix} p_3 \\ v_3 \end{bmatrix} \quad (9)$$

where

$$\alpha_{11} = \cos(k_s h_2) + j(Z_{p_1} + Z_f)/(\rho_s c_s) \cdot \sin(k_s h_2)$$

$$\begin{aligned} \alpha_{12} = & j\rho_s c_s \sin(k_s h_2) + Z_{p_3} \cos(k_s h_2) + \\ & + (Z_{p_1} + Z_f) \cos(k_s h_2) + \\ & + j Z_{p_3} (Z_{p_1} + Z_f) \sin(k_s h_2)/(\rho_s c_s) \end{aligned}$$

$$\alpha_{21} = j \sin(k_s h_2)/(\rho_s c_s)$$

$$\alpha_{22} = j Z_{p_3} \sin(k_s h_2)/(\rho_s c_s) + \cos(k_s h_2)$$

We see that if $(k_s h_2)$ is small (low frequency or a stiff spring), then $\cos(k_s h_2) \approx 1$ and $\sin(k_s h_2) \approx k_s h_2$. Considering $K = (\rho_s c_s^2)/h_2$, we find that equation (8) degenerates to (4) and (9) degenerates to (6) for small $(k_s h_2)$.

From equation (9) we get:

$$\begin{aligned} \frac{p_1}{v_3} = & \cos(k_s h_2) \{ (Z_{p_1} + Z_f) + (Z_{p_3} + Z_f) \} \\ & + j \sin(k_s h_2) \{ \rho_s c_s + (Z_{p_1} + Z_f)(Z_{p_3} + Z_f)/(\rho_s c_s) \} \end{aligned} \quad (10)$$

Now in analogy with our previous calculations in part 2.3.2 (Excitation by a point force) we can evaluate a new expression for equation (2.3.26):

$$W'_{II} = \frac{\rho c \omega^2 \hat{F}_{II}^2}{4\pi} k^2 \int_0^1 \frac{\xi (1 - \xi^2)^{-1/2} d\xi}{|\cos(k_S h_2) [N_1 + N_3] + j \sin(k_S h_2) [N_2^* + N_1 N_3 / N_2^*]|^2} \quad (11)$$

Here N_1 and N_3 are given by the same expressions as in part 2.3.2:

$$N_1 = [B_1 (k\xi)^4 - \omega^2 m_1] + j \left[\frac{\omega \rho c}{\sqrt{1 - \xi^2}} + B_1 \eta_1 (k\xi)^4 \right] \quad (12)$$

$$N_3 = [B_3 (k\xi)^4 - \omega^2 m_3] + j \left[\frac{\omega \rho c}{\sqrt{1 - \xi^2}} + B_3 \eta_3 (k\xi)^4 \right] \quad (13)$$

N_2 is however defined differently

$$N_2^* = j \omega \rho_S c_S = j \omega^2 \rho_S / k_S \quad (14)$$

To introduce losses in the interlayer we introduce a complex wavenumber (and a corresponding complex velocity):

$$k_S \rightarrow k_S (1 - j \eta_2 / 2) \quad (15)$$

The complex trigonometric functions can then be written as:

$$\cos(k_S h_2) \rightarrow \cos(k_S h_2) \cosh(k_S h_2 \eta_2 / 2) + j \sin(k_S h_2) \sinh(k_S h_2 \eta_2 / 2) \quad (16)$$

$$\sin(k_S h_2) \rightarrow \sin(k_S h_2) \cosh(k_S h_2 \eta_2 / 2) - j \cos(k_S h_2) \sinh(k_S h_2 \eta_2 / 2) \quad (17)$$

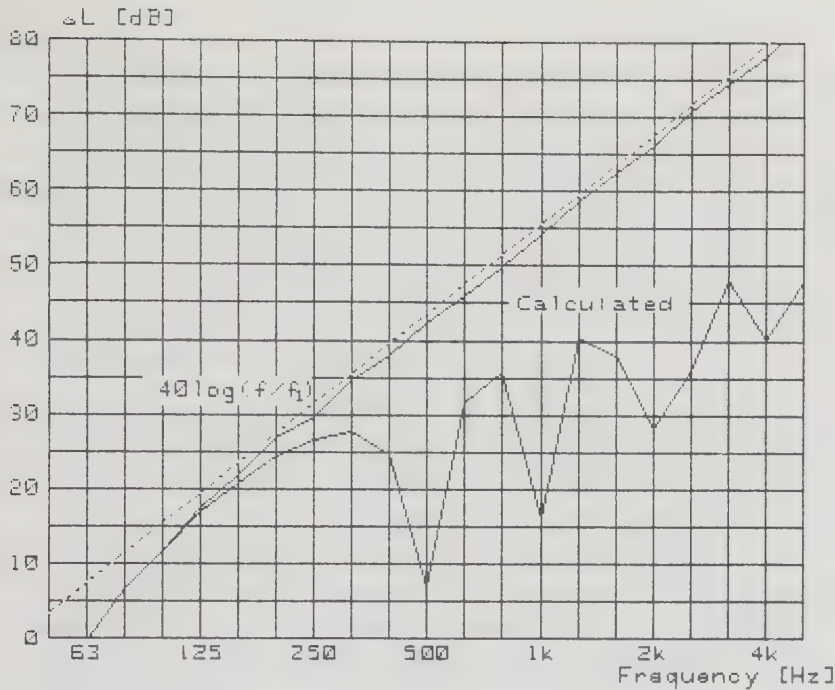
Now we assume equal point forces as before and get an expression for the impact level improvement:

$$\Delta L = 10 \log \frac{\int_0^1 \frac{\xi d\xi}{|N_3 + N_0| \sqrt{1 - \xi^2}}}{\int_0^1 \frac{\xi (1 - \xi^2)^{-1/2} d\xi}{|\cos(k_S h_2) [N_1 + N_3] + j \sin(k_S h_2) [N_2^* + N_1 N_3 / N_2^*]|^2}} \quad (18)$$

This expression has been solved numerically for several examples. The results are shown below, compared to our previous model, where the interlayer was considered to be massless springs.

As a fundamental example we continue to use 160 mm and 50 mm concrete slabs, and a 50 mm interlayer with a dynamic modulus four times the isothermal modulus of air.

Now the losses in the interlayer play a more significant role than before, and we show calculations for two different values of η_2 . First a very low value, $\eta_2 = 0.02$ and then our previously chosen (more realistic) $\eta_2 = 0.20$.

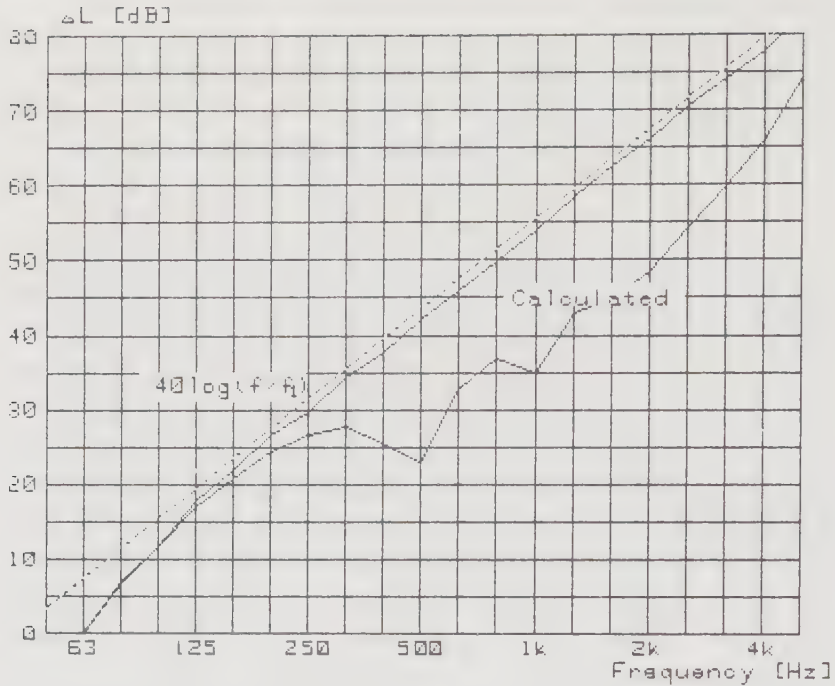


IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.020	160	0.4
	0.160	0.050	2400	30000

Figure 4.2.1. ΔL calculated from equation (18) and equation (2.3.28) respectively.

We see that the curves give the same result at low frequencies, but then our new curve turns off, and the maxima have a slope of about 20 dB/decade instead of 40 dB/decade. We also have deep resonance valleys at the frequencies $f_n = n c_s / (2h_2)$. Note that not all the resonances are visible in the figure, as the calculations have only been made at the 1/3-octave center frequencies (we see clearly the 1:st, 2:nd, 4:th and 8:th).



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200	160	0.4
	0.160	0.050	2400	30000

Figure 4.2.2. ΔL calculated from equation (18) and equation (2.3.28) respectively.

In figure 4.2.2 above we have increased the loss factor of the interlayer 10 times and kept all other parameters unaltered. We see that compared to figure 4.2.1 the resonance valleys are much more shallow, and another feature is the steep rise at high frequencies. The first three maxima have just about the same level and slope as before, but then the steep high frequency rise takes over.

Calculations have been made for different widths of the interlayer, different qualities of the mineral wool and several different slab configurations. However, in order to avoid drowning in too many curves, we show no more examples here. The general picture is clear from the two figures 4.2.1 and 4.2.2 and it is sufficient to describe verbally the changes from these caused by parameter variation.

Changing the width of the interlayer does not change the asymptote for the maxima. The resonance frequencies (including the fundamental resonance) are changed in such a way that a smaller width gives higher resonance frequencies and the interval between them is lengthened. The steep high frequency slope is moved to higher frequencies. A larger width gives the opposite effects.

Changing the modulus of the interlayer gives a new asymptote for the maxima, parallel to the first one, and the resonance frequencies are also moved. A lower modulus gives an asymptote at a higher level and lower resonance frequencies with shortened intervals. The steep high frequency slope also sets in at lower frequencies. A higher modulus gives the opposite effects.

Changing the floating slab changes only the level of the asymptote and the fundamental resonance frequency. A lighter slab gives an asymptote at a lower level and moves the fundamental frequency upwards. The opposite is true for a heavier slab.

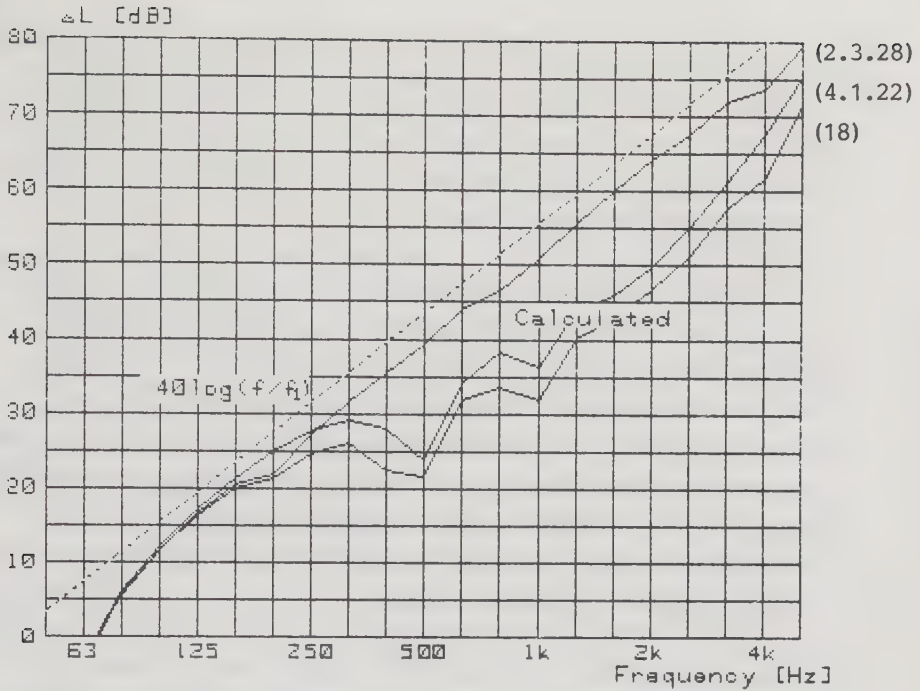
Changing the structural slab has very little influence if the slabs are sufficiently dissimilar both before and after the change. However if we consider a construction with very dissimilar slabs and change the structural slab so that it becomes considerably

more similar to the floating slab, then the asymptote is moved to a lower level. This is the same effect as was previously noted when figure 2.3.2.2 was compared to figure 2.3.2.1.

It is of course interesting to compare our results to the equations presented by Lindblad [22]. We have compared the equation (4.1.22) to our equation (18), and we find that the results are identical if the slabs are sufficiently dissimilar.

We now start off with slabs that are so dissimilar, that the two results are identical, and then we change the structural slab so that it successively becomes more and more similar to the floating slab. Then Lindblad's equation (4.1.22) always gives the same result, but equation (18) gives successively lower ΔL .

Below we show an example where the slabs are relatively similar ($f_{C_3}/f_{C_1} \approx 0.5$). It is our previous example of 200 mm slab of lightweight concrete and a 50 mm concrete slab. The calculations are first made as though simple bending wave theory was applicable for the entire frequency region, and this gives:



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200	160	0.4
	0.200	0.040	850	2250

Figure 4.2.3. ΔL calculated from equations (18) and (4.1.22), compared to equation (2.3.28).

Both the curves (2.3.28) and (18) show a deviation from $40 \log (f/f_1)$ and (4.1.22) respectively. We have previously explained this deviation as the second term of the forced solution (see eq. 2.2.6), which amounts to:

$$\Delta \Delta L = 10 \log \left(\left(\frac{k_{B_3}}{k_{B_1}} \right)^4 - 1 \right)^2 \quad (19)$$

We have assumed simple bending wave theory in calculating the curves in figure 4.2.3.

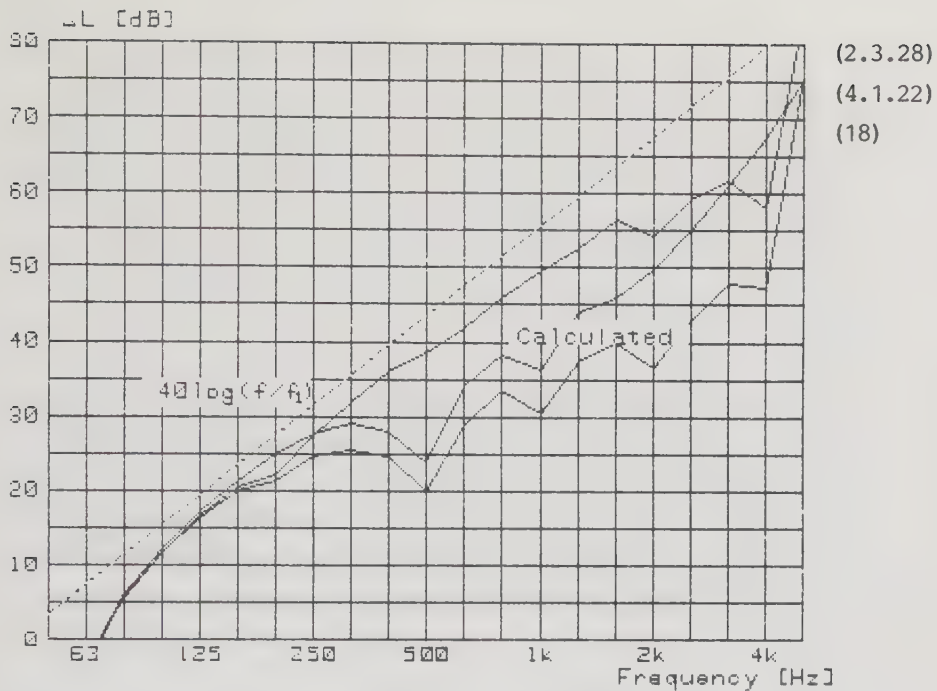
Accordingly the correction term (19) can be calculated as $\Delta\Delta L = 10 \log ((f_{c_3}/f_{c_1})^2 - 1)^2 = -2.5 \text{ dB}$. The deviations in figure 4.2.3 seem to be slightly larger than this correction (about -3 or -4 dB), but no other explanation has been found.

Our example above is perhaps a little unfortunate, as we have previously pointed out that the simple bending wave theory is only applicable up to about 500 Hz for this structural slab. In principle our comments above are correct, but as a more appropriate example for the case $f_{c_3}/f_{c_1} \approx 0.5$ we should perhaps have chosen a 10 cm structural slab of dense concrete. (Our lightweight example is one of the configurations in the laboratory measurements.)

In part 3.4.2 we suggested a rather coarse method to estimate the flexural wave velocity for frequencies above the limit for simple bending waves. This estimate was supposed to be used in estimating (k_{B_3}/k_{B_1}) in this frequency region. We now take another step, which is perhaps a little bit more daring. We introduce this simple correction into the equations (18) and (2.3.28). Instead of using the proper differential equation for corrected bending waves, we simply use the simple bending wave equation with a reduced bending stiffness B' . This new bending stiffness is chosen so that it gives the corrected flexural wave velocity c_B' from equation (3.4.8) when the simple bending wave equation is solved:

$$\begin{aligned}
 B' &= B / (1 + (c_B/c_T)^4) \\
 &= \frac{B}{1 + \frac{\omega^2 m h (1 + \mu)^2}{3E(1 - \mu^2)}}
 \end{aligned} \tag{20}$$

Below we show the example from figure 4.2.3 with this "corrected bending stiffness".



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200	160	0.4
	0.200	0.040	850	2250

Figure 4.2.4. Same as figure 4.2.3, but with the corrected bending stiffness from equation (20).

The calculated curve in the middle is Lindblad's equation (4.1.22) and it is not affected by the new bending stiffness. The two other curves are unchanged at low frequencies as compared to figure 4.2.3. At higher frequencies however we see larger deviations from the reference curves than in figure 4.2.3.

The correction term of equation (19) now increases smoothly from -2.5 dB at low frequencies up to about -8 dB at 2000 Hz. At about 3000 Hz the plates have identical wave numbers, so equation (19) should not be used for higher frequencies here than about 2000 Hz. Up to this frequency equation (19) may be said to agree reasonably well with the deviations in figure 4.2.4.

New curves with B' have also been calculated for the 160 mm concrete slab, but the effect is shown to be only marginal up to 4000 Hz. The correction term (19) is -2 dB at 5000 Hz instead of -1 dB at low frequencies. Thus it seems to be sufficiently accurate here to use the old curves (e.g. 4.2.1 and 4.2.2).

4.3 Anisotropy of the interlayer

Until now we have assumed the interlayer to be locally reacting. This seems to be justified if we consider that the velocity of the structural wave in the mineral wool is very much lower than the velocity of sound in air. For a dense mineral wool this velocity is about 50 m/s and the interlayer wave is therefore always just about normal to the plate. This is even more true for lighter qualities of mineral wool, as the velocity is lower there.

Let us assume that we have an incident airborne wave at an angle β . It excites a wave with the same trace velocity in the interlayer, and this wave has an angle β_s to the plate-normal.

For a homogeneous interlayer with $c_s = 50$ m/s we have $c/c_s \approx 7$, and Snell's law of refraction gives:

$$\beta_s = \arcsin (\sin \beta \cdot c_s / c) \quad (1)$$

Even for $\beta = 90^\circ$ we have β_s as low as 8° for this dense mineral wool.

We know however that the mineral wool is not isotropic. It has much higher stiffness parallel to the direction of the fibres than in the normal direction. The dynamic modulus is of the order of magnitude 10 times larger in the parallel direction, which means that the velocity is about 3 times higher in this direction. (See e.g. [36].)

Widén [23] assumes that the dynamic modulus for mineral wool can be written as:

$$D_s(\beta_s) = D_{s0} (\cos \beta_s + 10 \sin \beta_s) \quad (2)$$

If we use this expression we may write:

$$c_s(\beta_s) = c_{s_0} (\cos \beta_s + 10 \sin \beta_s)^{1/2} \quad (3)$$

where c_{s_0} is the velocity in the normal direction. This together with equation (1) gives us:

$$\beta_s = \arcsin \frac{10 + \sqrt{100 + 4(1 + \alpha_s^2)}}{2(1 + \alpha_s^2)} \quad (4)$$

where

$$\alpha_s = c / (c_{s_0} \cdot \sin \beta)$$

Here we still have the wave in the interlayer almost perpendicular to the plate. For $\beta = 90^\circ$ and $c/c_s = 7$ we get $\beta_s = 16^\circ$. Even though this is a small angle it gives $c_s = 1.9 c_{s_0}$ according to equation (3) and a correspondingly lower wavenumber.

We would like to see how this influences our solution from part 4.2. To do this we now no longer assume the interlayer to be locally reacting. Instead of the wavenumbers k_s in equation (4.2.18) we now calculate with the projected wavenumbers

$$k_{sx} = \frac{\omega}{c_s} \cos \beta_s \quad (5)$$

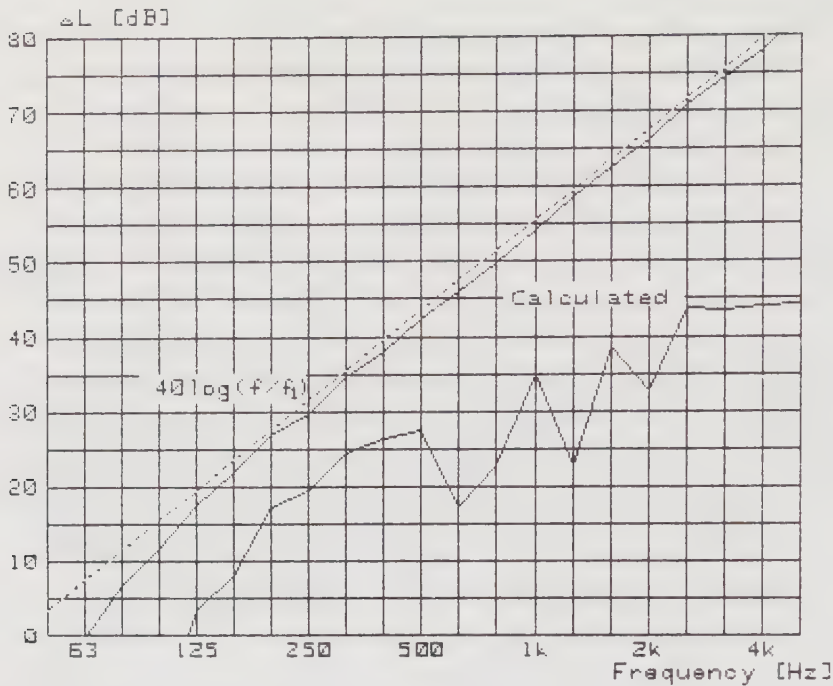
If we consider the interlayer at first to be isotropic with the velocity $c_s = c_{s_0}$ in all directions, we have

$$k_{sx} = k_s \sqrt{1 - \left(\frac{k}{k_s} \sin \beta\right)^2} \quad (6)$$

Now $\sin \beta$ is just the variable ξ in equation 4.2.18 so this projection is easily made. The results for this isotropic model are found to be identical to our previous results for the locally reacting model.

However, with the anisotropic model described in equation (2), considerable changes are found. Here we evaluate k_{sx} in equation (5) with the help of equations (3) and (4).

As an example we show the same case as presented as locally reacting in figure 4.2.1. The loss factor of the interlayer is very low here and figure 4.2.1 has deep resonance valleys. If we compare this to our figure 4.3.1 we find principally the same changes as a higher dynamic modulus would have caused. The asymptote of the maxima is lowered somewhat, and the resonances moved to higher frequencies, particularly the fundamental resonance. We may also note that the curve in figure 4.3.1 is more smooth, the resonance valleys are more shallow than before.



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

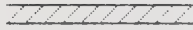
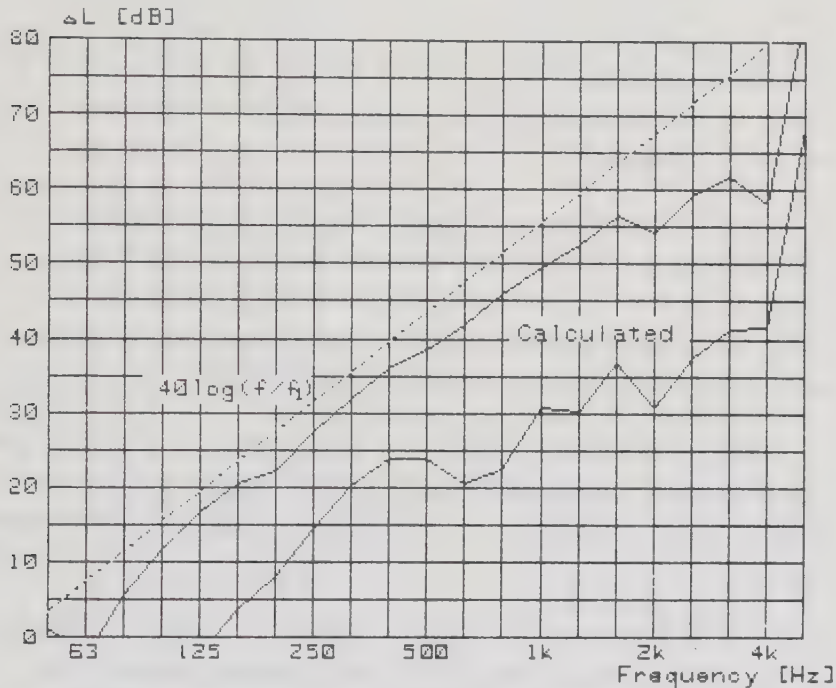
	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.020	180	0.4
	0.180	0.050	2400	30000

Figure 4.3.1. ΔL calculated from equation (4.2.18) with projected wavenumbers and an anisotropic inter-layer according to equation (2).

Similar changes have been experienced for other configurations. As another example we show the curve for the configuration in 4.2.4, corrected with B' and with projected wavenumbers in an anisotropic interlayer:



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200	160	0.4
	0.200	0.040	850	2250

Figure 4.3.2. ΔL calculated from equation (4.2.18) with the modified bending stiffness of equation (4.2.20), and with projected wavenumbers in the anisotropic interlayer from equation (2).

4.4 SEA considerations

As previously mentioned, Vêr [2] and Ljunggren [18] have both made SEA calculations on floating floors. Their respective models of the interlayer seem however too abstract to suit our practical example of a continuous mineral wool. A large number of different problems have been tackled with SEA in numerous publications. We have already mentioned [19], but we have mainly consulted [29] in our work here.

It may seem strange to place the SEA considerations here in chapter 4, as the transmission is obviously resonant which was basically treated in chapter 3. However, we have modelled the interlayer as a wave medium, which in turn led to this placing.

The SEA method only considers the resonant part of the transmission. We know however, that there is forced transmission as well, and that part must be considered too.

We may suggest that we follow Cremer's forced solution up to a frequency, where the resonant transmission according to SEA is larger than the forced transmission. Above that frequency the SEA solution may be used.

Our SEA model of the floating floor is made up of three subsystems, one for each plate and one for the interlayer. This is compared to the system of the structural slab by itself.

The analysis has been tried in both directions, i.e. primary excitation of the floating slab and the structural slab respectively. It turns out that some

of the parameters are more easily simplified when working "upwards", that is considering the structural slab to be primarily excited.

It has previously been concluded that the results should be independent of this choice of direction, and we only show this latter analysis here. In the figure below we show our SEA systems and subsystems:

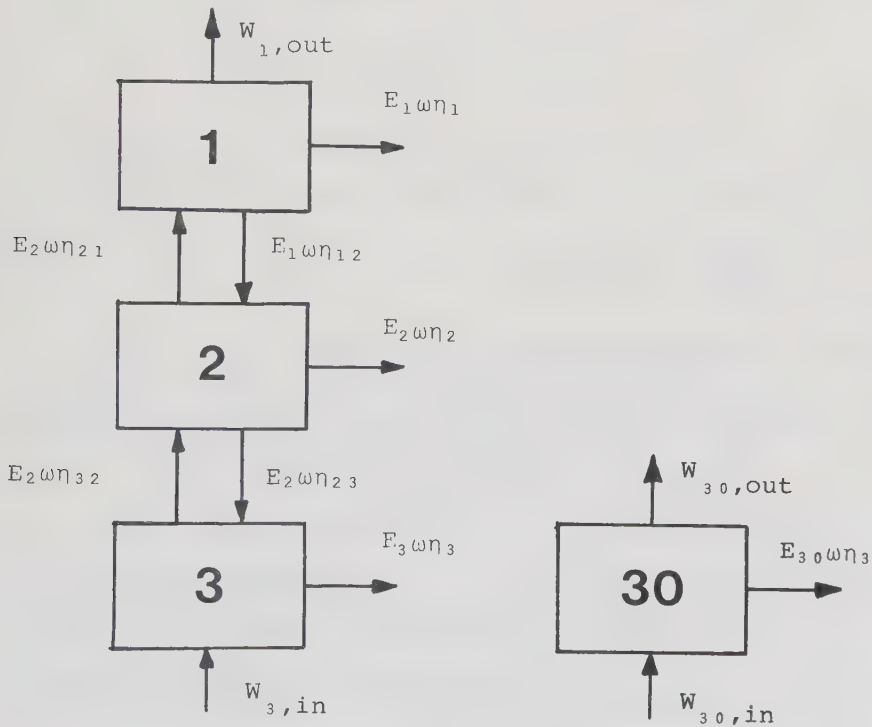


Figure 4.4.1. The SEA systems: 1 is the floating slab, 2 is the interlayer, 3 is the structural slab and 30 is the structural slab by itself.

W stands for power, E_i stands for the energy of system i , η_i is the loss factor of system i and η_{ij} is the coupling loss factor of system i to system j .

At first we consider the floating floor construction, and start with the power balance of each subsystem:

$$W_{1,out} + E_1\omega(\eta_1 + \eta_{12}) = E_2\omega\eta_{21} \quad (1)$$

$$E_2\omega(\eta_2 + \eta_{21} + \eta_{23}) = E_1\omega\eta_{12} + E_3\omega\eta_{32} \quad (2)$$

$$W_{3,in} = E_3\omega(\eta_3 + \eta_{32}) - E_2\omega\eta_{23} \quad (3)$$

In (1) we can in most cases assume $W_{1,out}$, the radiated power, to be a negligible term. This gives us:

$$\frac{E_1}{E_2} = \frac{\eta_{21}}{\eta_1 + \eta_{12}} \quad (4)$$

With (4) inserted into (2) we get:

$$\frac{E_2}{E_3} = \frac{\eta_{32}(\eta_1 + \eta_{12})}{(\eta_2 + \eta_{21} + \eta_{23})(\eta_1 + \eta_{12}) - \eta_{12}\eta_{21}} \quad (5)$$

Combining (4) and (5) now gives:

$$\frac{E_1}{E_3} = \frac{E_1}{E_2} \frac{E_2}{E_3} = \frac{\eta_{21}\eta_{32}}{\eta_1(\eta_2 + \eta_{21} + \eta_{23}) + \eta_{12}(\eta_2 + \eta_{23})} \quad (6)$$

Let us now consider the different parameters in (6). We have:

$$E_1 = m_1 S \langle \tilde{v}_1^2 \rangle \quad (7)$$

$$E_3 = m_3 S \langle \tilde{v}_3^2 \rangle \quad (8)$$

The loss factors η_1 , η_2 , η_3 are defined for the uncoupled systems. The coupling loss factors are the most difficult parameters to estimate. If we consider the interlayer to be a homogeneous wave medium with

ρ_s and c_s , we may write:

$$\sigma_{s1} \rho_s c_s \langle \tilde{v}_1^2 \rangle_S = \omega \eta_{12} m_1 \langle \tilde{v}_1^2 \rangle_S \quad (9)$$

or

$$\eta_{12} = \frac{\rho_s c_s}{\omega m_1} \sigma_{s1} \quad (10)$$

Because of the low velocity c_s , we have the radiated wave almost perpendicular to the plate. σ_{s1} can therefore always be estimated with $\sigma_{s1} \approx 1$ in the frequency region where the SEA considerations are valid:

$$\eta_{12} \approx \frac{\rho_s c_s}{\omega m_1} \quad (11)$$

$$\eta_{32} \approx \frac{\rho_s c_s}{\omega m_3} \quad (12)$$

The opposite coupling loss factors from the mineral wool to the slabs are more difficult to estimate.

However system 1 usually has low inner losses. It may be a reasonable assumption that these losses are so small that system 1 reaches the same acoustic temperature (modal energy) as system 2.

For this situation we have

$$n_2 \eta_{21} = n_1 \eta_{12} \quad (13)$$

where n now stands for the modal density ($n = \Delta N / \Delta f$). Inserting (13) into (6) gives us:

$$\frac{E_1}{E_3} = \frac{(n_1/n_2) \eta_{12} \eta_{32}}{n_1 (\eta_2 + (n_1/n_2) \eta_{12} + \eta_{23}) + \eta_{12} (\eta_2 + \eta_{23})} \quad (14)$$

We still have the coupling loss factors η_{23} here, the power flow back to the directly excited system. If we further assume that system 2 has high inner damping, we may neglect this flow back to system 3 and (14) can be written:

$$\frac{E_1}{E_3} = \frac{(n_1/n_2) \eta_{12} \eta_{32}}{\eta_1 (\eta_2 + (n_1/n_2) \eta_{12}) + \eta_{12} \eta_2} \quad (15)$$

Inserting (7), (8), (11), (12) into (15) gives:

$$\begin{aligned} \Delta L_V &= 10 \log (\langle \tilde{V}_3^2 \rangle / \langle \tilde{V}_1^2 \rangle) \\ &= 20 \log \left(\frac{\omega m_1}{2 \rho_S c_S} \right) + 6 \text{ dB} + \Delta L_{\eta_1} \end{aligned} \quad (16)$$

where

$$\Delta L_{\eta_1} = 10 \log \left[\frac{n_2}{n_1} \eta_2 \left(\eta_2 + \frac{\rho_S c_S}{\omega m_1} \right) + \eta_1 \frac{\rho_S c_S}{\omega m_1} \right]$$

We would now like to compare $\langle \tilde{V}_1^2 \rangle$ above to the value $\langle \tilde{V}_{3,0}^2 \rangle$ we would get in the absence of systems 1 and 2.

The relation between $\langle \tilde{V}_3^2 \rangle$ and $\langle \tilde{V}_{3,0}^2 \rangle$ may be found by comparing the following two equations:

$$\langle \tilde{V}_3^2 \rangle = \frac{W_{3,\text{in}}}{\omega \eta_{3,\text{tot}} m_3 S} = \frac{\tilde{F}_3^2}{8c^2 m_3^2 S \eta_{3,\text{tot}} f/f_{c_3}} \quad (17)$$

$$\langle \tilde{V}_{3,0}^2 \rangle = \frac{W_{3,0,\text{in}}}{\omega \eta_3 m_3 S} = \frac{\tilde{F}_{3,0}^2}{8c^2 m_3^2 S \eta_3 f/f_{c_3}} \quad (18)$$

In equation (18) we have neglected the radiation losses into the air as before. In (17) $\eta_{3,\text{tot}}$ is the total loss factor including the coupling losses to system 2, and we have neglected the power flow back to system 3 once again. If we assume that the same force is applied in both cases ($F_3 = F_{3,0}$) we have:

$$\frac{\langle \tilde{V}_3^2 \rangle}{\langle \tilde{V}_{3,0}^2 \rangle} = \eta_3 / \eta_{3,\text{tot}} \quad (19)$$

The total loss factor is:

$$\begin{aligned} \eta_{3,\text{tot}} &\approx \eta_3 + \eta_{32} \\ &= \eta_3 + \frac{\rho_S c_S}{\omega m_3} \end{aligned} \quad (20)$$

Now the sound insulation improvement can be written as:

$$\begin{aligned}\Delta L &= 10 \log \frac{\langle \tilde{v}_{30}^2 \rangle \sigma_3}{\langle \tilde{v}_1^2 \rangle \sigma_1} \\ &= 20 \log \left(\frac{\omega m_1}{2 \rho_s c_s} \right) + 6 \text{ dB} + \Delta L_{\eta_1} + \Delta L_{\eta_3} + \Delta L_{\sigma}\end{aligned}\quad (21)$$

$$\text{where } \Delta L_{\eta_3} = 10 \log \left(1 + \frac{\rho_s c_s}{\omega m_3 \eta_3} \right)$$

$$\Delta L_{\eta_1} \text{ as in equation (16)}$$

$$\Delta L_{\sigma} = 10 \log (\sigma_3 / \sigma_1)$$

We see that equation (21) has certain similarities to the expressions cited in part 4.1, and some numerical comparison might be interesting.

To evaluate ΔL_{η_1} , the modal densities n_1 and n_2 must first be estimated. n_1 is the modal density for bending waves in a plate, and can be written as (see [6] e.g.)

$$n_1 = \frac{S f c_1 \pi}{c^2} \quad (22)$$

The modal density of the interlayer is more difficult to estimate. In [19] a two-dimensional space (narrow cavity) is considered and the modal density is found to be

$$n_2 = \frac{2 S f \pi}{c_s^2} \quad (23)$$

The expression is only valid up to $f = c_s / 2h_2$, and it is obviously not valid here, as our waves are almost perpendicular to the plates, and the modes in (23) are parallel to the plates.

Considering that the waves are almost perpendicular to the plates we might consider to use the modal density of a one-dimensional room

$$n_2 = 2h_2/c_s \quad (24)$$

This modal density is however extremely low, and it leads to unrealistically low values for ΔL . Besides it is doubtful if the SEA method is applicable for such a low modal density. We try instead the method used in chapter 3 to limit the number of excited modes by considering the limited angle of incidence. This is however a little bit more difficult here, as we have a three-dimensional modal space.

The usual expression for the number of modes in a room is:

$$N = \frac{4\pi V' f^3}{3c^3} + \frac{\pi S' f^2}{4c^2} + \frac{L' f}{8c} + \frac{1}{8} \quad (25)$$

where V' is the volume, S' the total surface area and L' the total length of all edges in the room.

The most convenient way to deduce the above equation is to consider a three-dimensional grid of modes. Then an 1/8-sphere with the radius f is drawn and the modes that lie inside this 1/8-sphere are counted.

Here, we are confined to the modes in this 1/8-sphere, that lie within the conical segment around one of the axes with a top-angle $\beta = \arcsin(c_s/c)$. See the figure below:

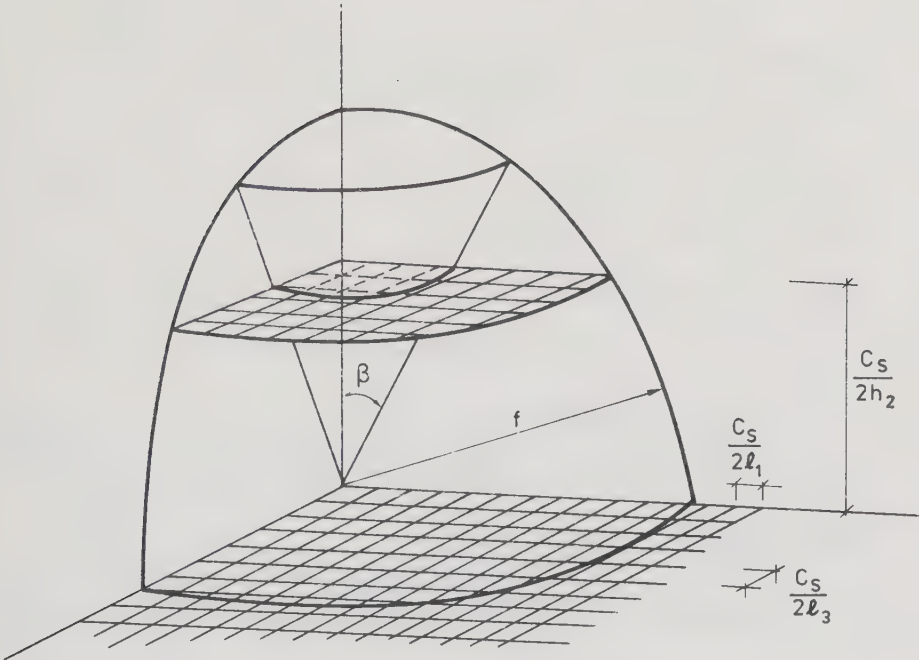


Figure 4.4.2. The modal space, h_2 is the width of the interlayer and $l_1 l_3 = S$ is the area of the slabs.

The volume of this conical segment is $(1 - \cos \beta)$ of the 1/8-sphere, so the first term in (25) becomes here:

$$\frac{4\pi h_2 S f^3}{3C_S^3} (1 - \cos \beta)$$

In the second term we only consider the four sides of the room with the height h_2 . The total area of these is $U \cdot h_2$ and we are confined to only $\beta/(\pi/2)$ of these, giving:

$$\frac{\pi U h_2 f^2}{4 c_s^2} \cdot \frac{2\beta}{\pi}$$

In the third term we only consider the four edges which have the length h_2 , giving

$$\frac{h_2 f}{2 c_s}$$

The last term is unaltered.

If we now consider that $\beta = \arcsin(c_s/c) \approx c_s/c$ and $1 - \cos \beta = 1 - (1 - (c_s/c)^2)^{1/2} \approx (c_s/c)^2/2$, we may write:

$$N_2 = \frac{2\pi h_2 S f^3}{3 c_s c^2} + \frac{h_2 U f^2}{2 c_s c} + \frac{h_2 f}{2 c_s} + \frac{1}{8} \quad (26)$$

This gives the modal density:

$$n_2 = \frac{\Delta N_2}{\Delta f} = \frac{2\pi h_2 S f^2}{c_s c^2} + \frac{h_2 U f}{c_s c} + \frac{h_2}{2 c_s} \quad (27)$$

We can see that the first term dominates in our frequency region of interest, and we may write:

$$n_2 \approx \frac{2\pi h_2 S f^2}{c_s c^2} \quad (28)$$

We can now write ΔL_{η_1} from equation (16) as:

$$\Delta L_{\eta_1} = 10 \log \left[\frac{2f^2 h_2 \eta_2}{c_s f_{c_1}} \left(\eta_1 + \frac{\rho_s c_s}{\omega m_1} \right) + \eta_1 \frac{\rho_s c_s}{\omega m_1} \right] \quad (29)$$

We see that for a well damped floating slab with large and frequency independent η_1 , ΔL_{η_1} increases approximately as 20 dB/decade.

For a low η_1 , where $\rho_s c_s / \omega m_1$ may dominate we have an increase in ΔL_{η_1} as 10 dB/decade. (Frequency independent η_2 assumed in both cases.)

To get the total slope of ΔL we must now consider the

other terms in equation (21), which we rewrite here as equation (30):

$$\Delta L = 20 \log \left(\frac{\omega m_1}{2 \rho_s c_s} \right) + 6 \text{ dB} + \Delta L_{\eta_1} + \Delta L_{\eta_3} + \Delta L_{\sigma} \quad (30)$$

$$\text{where } \Delta L_{\eta_1} = 10 \log \left(\frac{2 f^2 h_2 \eta_2}{c_s f_{c_1}} \left(\eta_1 + \frac{\rho_s c_s}{\omega m_1} \right) + \eta_1 \left(\frac{\rho_s c_s}{\omega m_1} \right) \right)$$

$$\Delta L_{\eta_3} = 10 \log \left(1 + \rho_s c_s / (\omega m_3 \eta_3) \right)$$

$$\Delta L_{\sigma} = 10 \log (\sigma_3 / \sigma_1)$$

The two terms ΔL_{η_3} and ΔL_{σ} can be considered negligible above f_{c_1} . What then is left is the 20 dB/decade slope of the first term to be added to the slope of ΔL_{η_1} above, giving a total slope of 30 - 40 dB/decade approximately. The higher slope for a well damped floating slab, and the lower slope for a lightly damped slab.

We would now like to compare equation (30) to our previous results.

We choose Lindblad's equation (4.1.22), which we have shown to give the same result, for sufficiently dissimilar slabs, as our extended Fourier transform method. To compare this equation with the SEA-equation it must however first be complemented with the resonant correction term from chapter 3. After this correction we can rewrite equation (4.1.22) as

$$\begin{aligned} \Delta L = & 20 \log \left(\frac{\omega m_1}{2 \rho_s c_s} \right) - 10 \log |G| - 10 \log (1 + Y \sigma_F) \\ & + 10 \log \left((1 - |G| \cos \varphi)^2 + (|G| \sin \varphi)^2 \right) \quad (31) \end{aligned}$$

$$\text{where } |G| = e^{-k_s \eta_2 h_2}$$

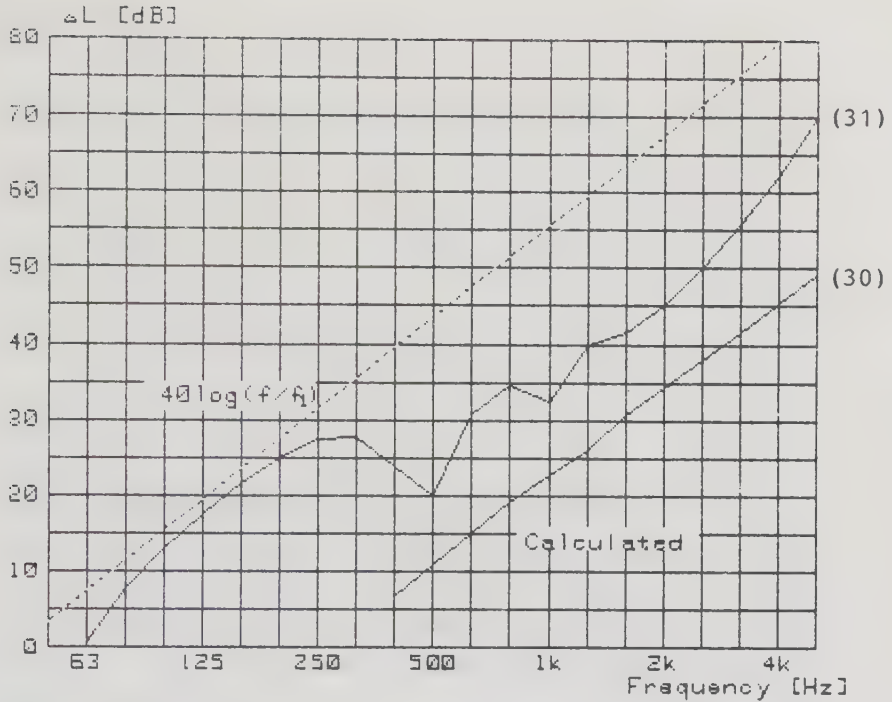
$$\varphi = (1/m_1 + 1/m_3) 2 \rho_s c_s / \omega - 2 h_2 k_s$$

Y: See table 3.3.3.4.

$$\sigma_F \approx \begin{cases} 0 & f < f_{c_1} \\ 1 & f > f_{c_1} \end{cases}$$

As a practical example we choose once again our fundamental configuration, 160 mm, 50 mm concrete slabs and a 50 mm interlayer with a dynamic modulus 4 times the isothermal one for air. The loss factors may be chosen as 1 % for the floating slab, 5 % for the structural slab and 20 % for the interlayer. U/S we choose as 14/12 and the resonant factor Y can be estimated as $0.03/\eta_1$ (see table 3.3.3.4). Here η_1 should really be $\eta_{1,tot}$, that is the loss factor we would measure for the slab on top of the mineral wool. In these SEA considerations we have assumed $\eta_{1,tot} = \eta_1 + \rho_s c_s / (\omega m_1)$, and in the following comparison between the two methods we have used this $\eta_{1,tot}$ to calculate Y.

We have only drawn the SEA curve from f_{c_1} and upwards. Below f_{c_1} the forced solution should dominate.



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

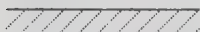
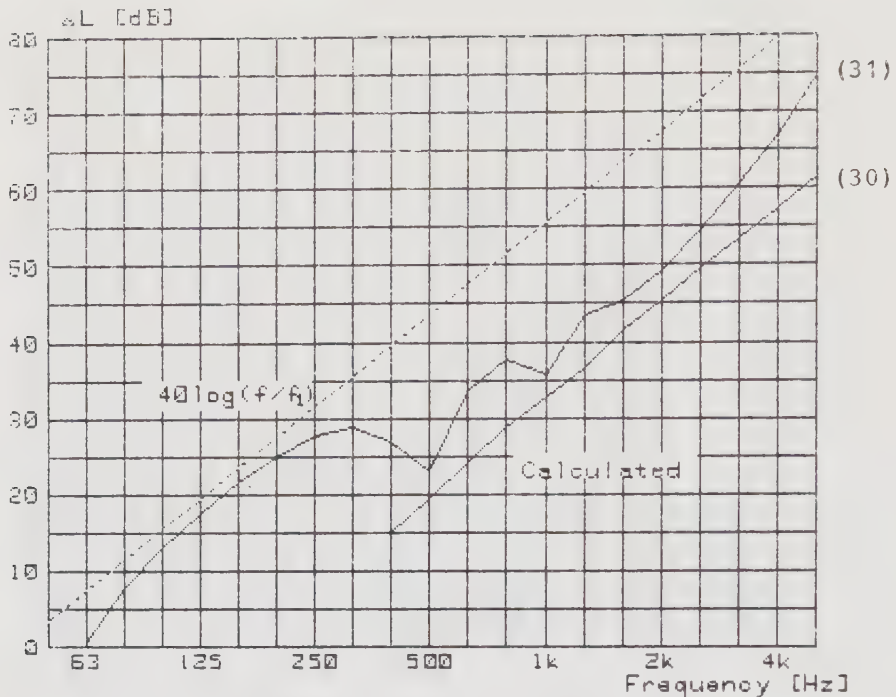
	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.010	2400	30000
	0.050	0.200	160	0.4
	0.160	0.050	2400	30000

Figure 4.4.3. Comparison between the SEA expression (30) and equation (31).

We see that for this poorly damped floating slab, the agreement between the two methods is not very good, the SEA method gives a much lower ΔL .

It may be interesting to see what happens if we replace the floating slab with a highly damped one. This is done in figure 4.4.4, where η_1 has been assumed to be as high as $\eta_1 = 20\%$.



IMPACT LEVEL IMPROVEMENT FOR A FLOATING FLOOR

	width [m]	loss fact	density [kg/m ³]	E-modulus [MPa]
	0.050	0.200	2400	30000
	0.050	0.200	180	0.4
	0.160	0.050	2400	30000

Figure 4.4.4. Same as in figure 4.4.3 but much higher n_1 .

The resonant correction term in equation (31) can now be completely neglected and both curves are moved to a higher level. Equation (30) increases more than (31), but still the agreement between these two methods cannot be said to be good.

In both the examples above we also have a problem in "sewing together" the SEA solution and the forced

solution, as the SEA solution gives higher transmission than the forced one.

Our suggestion at the beginning of these SEA considerations, that we might follow Cremer's forced solution up to a frequency where the SEA calculations give larger transmission, is difficult to apply. We might however follow the forced solution up to for example $f_{c_1}/2$ and then have a smooth transition over to the SEA solution above f_{c_1} . This gives a very deep valley at the critical frequency, especially for the slab with the low damping.

We may consider what happens to our two different solutions if the width of the interlayer is doubled. ΔL_{η_1} increases 3 dB so the SEA curve rises 3 dB above f_{c_1} . The other solution is differently affected, the asymptote for the maxima is unchanged, but the resonance minima are shifted to lower frequencies. At the higher frequencies, the damping term is doubled, which can raise the curve considerably there. At the lower frequencies f_1 is shifted to $f_1/\sqrt{2}$ and ΔL immediately above f_1 should rise approximately $40 \log \sqrt{2} = 6$ dB. (This applies to both solutions.)

5. LABORATORY MEASUREMENTS

5.1 Sound measurements

5.1.1 The laboratory

The sound insulation measurements were made in a laboratory intended for impact noise measurements. The laboratory consists of two rooms, "box upon box", with an opening in the floor between them. The floor is double with an elastic interlayer, and the test floor rests on a hard rubber strip in a frame, which is part of the extremely thick (500 mm) floor in the upper room. This arrangement makes the test unsensitive to flanking transmission as far as impact measurements are concerned. However there is a risk of flanking transmission when airborne sound is measured. The sound excites the walls, ceiling and floor of the upper room, and these vibrations are transmitted to the test object. This is usually not a problem when the structural slab is directly subjected to the airborne noise. However, when it is covered by a floating floor, this reduces the vibrations caused by sound incident on the testing area, and in this situation the flanking vibrations may become important.

The upper room is 90.2 m^3 , the lower room 99.2 m^3 , the test opening is $4.08 \times 2.98 \text{ m}^2$ and the width from the frame to the surface of the floor is 400 mm.

In each room there are diagonal wires along which the microphone travels, and measurements can be made in up to 10 fixed positions.

To increase the diffusion of the sound fields, there are a number of suspended diffusors in each room.

In the upper room there is a travelling crane, which can lift the different slabs from the test opening and move them out through a door.

5.1.2 The test objects

Two different structural slabs were used:

- (i) 160 mm homogeneous concrete slab with an extremely smooth and hard surface layer of an epoxy material (approximately 2 - 4 mm).
- (ii) 200 mm slab of lightweight concrete elements, around 3 m x 0.6 m each. The tongued and grooved joints between the elements were filled with mortar.

The edges were lined with mineral wool and sealed with rubber strips, and control measurements were made to ensure that leaking had been prevented.

Below is a table of the material properties of each slab. These were not measured directly, except the loss factors, but borrowed from textbooks and the manufacturer of the lightweight elements (Siporex AB):

Table 5.1.2 1. Material properties of the structural slabs.

	Density (kg/m ³)	Modulus of elasticity (Pa)	Poisson's ratio	Loss factor*
Dense concrete	2400	$3 \cdot 10^{10}$	0.25	$\eta \approx 0.058 \cdot (100/f)^{0.77} \quad f < 630 \text{ Hz}$ $\eta \approx 0.014 \quad f \geq 630 \text{ Hz}$
Lightweight concrete	850	$2.25 \cdot 10^9$	0.25	$\eta \approx 0.037 \cdot (1000/f)^{0.89}$

*The loss factors are measured in the absence of the floating constructions (see Appendix V.1.1 and V.1.2).

As floating floors, primarily two types were used:

- (i) 22 mm chip board, tongued and grooved at the joints, which were glued together.
- (ii) 50 mm slab of homogeneous concrete, cast in the test opening.

Both floor types could be lifted with the travelling crane.

As special variants we tried two and four layers of chip board, and also 350 x 350 mm concrete tiles with the same thickness, 50 mm, as the homogeneous slab.

Below is a table of the material properties of each type of the floating floors. As before the values are borrowed, except the loss factors and the densities of the chip board and concrete tiles.

Table 5.1.2.2. Material properties of the floating floors.

	Density (kg/m ³)	Modulus of elasticity (Pa)	Poisson's ratio	Loss factor*
Concrete slab	2400	$3 \cdot 10^{10}$	0.25	$\eta \approx 0.016 \cdot (1000/f)^{0.26}$
Chip board	700	$3.5 \cdot 10^9$	0.25	$\eta \approx 0.033 \cdot (1000/f)^{0.90}$ $f < 1250$ Hz $\eta \approx 0.030$ $f \geq 1250$ Hz
Concrete tiles	2400	$3 \cdot 10^{10}$	0.25	$\eta \approx 0.014$ $f > 1000$ Hz

*The loss factors are measured for the floors resting on dense rockwool (see Appendix V.1.3, V.1.4, V.1.9).

Three different types of mineral wool were primarily tested as resilient interlayers.

(A) 50 mm Rockwool (389-00/50), 160 kg/m³

(B) 50 mm Rockwool (335-00/50), 100 kg/m³

(C) 50 mm Gullfiber (1275), glass fibre 50 kg/m³

To investigate the influence of different widths of the interlayer, we wanted a series of doubled and redoubled widths. To make this possible we used 30 mm layers of 160 kg/m³ Rockwool in single, double and quadruple layers. This type is not a standard product. Finally a thin (20 mm) interlayer of a sponge-like foam plastic (BF-80) was tested for comparison.

During the first measurements we were not aware of the fact that the properties of the mineral wool change considerably if it is not carefully treated when used over and over again. If it is stepped on, the mineral wool loses much of its original elasticity and instead of the stiff boards we end up with a more blanket-like material.

After realizing this, the measurement series was restarted with new mineral wool, which was carefully treated and kept in "unwalked" condition.

In Appendix III.2.1 we show an example of the difference in sound insulation between type A mineral wool in "unwalked" and "walked" condition. The "walked" type is called g (from Scanian "gådd" = walked upon).

Below is a table showing the different materials used as a resilient interlayer.

For a further description of the material properties we refer to part 5.4.

Table 5.1.2.3. Different interlayer materials.

Product	Our notation	Width (mm)	Measured density (kg/m ³)
Rockwool 389-00/50	A, g *)	50	159
Rockwool (special product)	a	30	162
Rockwool 335-00/50	B	50	96
Gullfiber 1275	C	50	54
Foam plastic BF-80	d	20	107

* A: carefully treated mineral wool, not stepped on (unwalked!).

g: not so carefully treated (walked mineral wool!).

5.1.3 Impact level measurements

The impact level measurements are made by measuring the sound level in the lower room with an ISO-tapping machine on the test floor in the upper room. The average level for 5 positions of the tapping machine and 3 microphone positions for each of them is found. This average level (L_m) is presented in a diagram together with the highest and the lowest values at each frequency and the background levels at the beginning and at the end of the measurement. An example

is shown in Appendix VII.1.1.

It shows a typical measurement, with a rather large spreading in the measured level at low frequencies and very little spreading at high frequencies. In spite of the spreading at low frequencies, acceptable reproducibility was found even at those frequencies. At high frequencies the difference between the measured level and background level usually becomes too small. If this difference is larger than about 3 dB, corrections to the measured level can be made. However, we have simply chosen to disregard all measurements that have this difference less than approximately 10 dB, and to keep the other results without this type of correction.

To obtain the normalized impact level according to [37] or [38], the reverberation time (T_{60}) in the receiving room has to be measured. This is done using the interrupted noise method, averaged over 10 microphone positions. An example is shown in Appendix VII.1.3. The normalized impact level is then calculated using the equation:

$$L_{10} = L_m + 10 \log \left(\frac{0.16 V}{T_{60} \cdot 10} \right) \quad (1)$$

Finally the impact sound index I_i is calculated according to [38]. The measurements are made in 1/3-octaves, usually in the frequency region 50 - 5000 Hz, but sometimes in a more narrow frequency region.

Primarily the impact level measurements are made directly on the untreated surfaces of the floating floors, and the structural slabs respectively. Some complementary measurements have also been made with a thin resilient floor covering (1.9 mm plastic carpet).

These measurements are only made on the configurations, which have the lightweight structural slab. To get a proper reference, the structural slab is also measured with the same type of plastic carpet.

In Appendix I the different measurement results are shown. Each diagram shows the reference measurement (L_{10} for the structural slab) and the measured L_{10} for the actual floating construction. In the same diagram we also show the difference, ΔL , which is the impact sound insulation improvement.

In Appendix I.1.1 - I.1.8 there are shown results for different configurations with the lightweight structural slab and without the plastic carpet:

	FLOATING FLOOR	INTERLAYER
I.1.1-3	Chip board	A, B, C
I.1.4-5	Concrete slab	A, B
I.1.6-7	Concrete tiles	A, B
I.1.8	Concrete tiles + chip board	B

In part I.2.1 - I.2.13 the results for different configurations with the lightweight structural slab and with the plastic carpet are shown.

The improvement ΔL is found by comparing the total construction to the structural slab with the same carpet:

	FLOATING FLOOR	INTERLAYER
I.2.1-3	Chip board	a 30, 60, 120 mm
I.2.4-6	Chip board 1x, 2x, 4x	A
I.2.7	Chip board	g
I.2.8	Chip board	d
I.2.9-10	Concrete slab	A, B
I.2.11-12	Concrete tiles	A, B
I.2.13	Concrete tiles + chip board	A

In part I.3.1 - I.3.13 the results for different configurations with the heavy structural slab are shown:

	FLOATING FLOOR	INTERLAYER
I.3.1-3	Chip board	A, B, C
I.3.4	Chip board	a 30 mm
I.3.5	Chip board	d
I.3.6-8	Concrete slab	A, B, C
I.3.9-11	Concrete slab	a 30, 60, 120 mm
I.3.9-12	Concrete slab	d
I.3.9-13	Concrete tiles	A

5.1.4 Airborne sound insulation measurements

The transmission loss measurements are made in accordance with [37] by measuring the difference in sound level ($L_s - L_m$) and correcting for the test area (S) and the absorption in the receiving room:

$$R = L_s - L_m + 10 \log \left(\frac{S \cdot T}{0.16 V} \right) \quad (2)$$

We average over 10 positions in each room and the background level in the receiving room is measured at the beginning and at the end of a measurement.

An example is shown in Appendix VII.1.2. As for the impact level measurements the highest and lowest measured values at each frequency are shown. We see again that the spreading is rather large at the lower frequencies, but very little at high frequencies. Also here, acceptable reproducibility has been found, even at low frequencies. We see that the measured background level is not a problem here, but we have another serious problem, which is the previously mentioned flanking transmission.

Measurements were made to estimate the dynamic range available for the measurements of airborne transmission loss. In these measurements the bottom room was used as a source room, exciting the 160 mm concrete slab in the test opening. The acceleration level of all radiating surfaces in the upper room was then measured, and the difference in level between the test slab and the flanking constructions calculated. After a correction for the different areas we get the result shown in Appendix II.3.1, curve (2).

The flanking constructions are mainly made of lightweight concrete, and have a lower radiation efficiency than the concrete slab up to approximately 300 Hz. Above that the radiation efficiencies should be equal, and we see that the dynamic range for the measurement of the airborne sound insulation improvement is very limited indeed.

In the same diagram (Appendix II.3.1) we also show the highest measured transmission loss improvement for a reinforced floating construction. The agreement between the two curves is not perfect, but it seems that our estimate of the available dynamic range is fairly good.

The conclusion must be that flanking transmission influences strongly the results of measured transmission loss improvements.

In Appendix II.1.1 - II.1.3 we show examples of three such measurements, and as we can see, we get very much the same improvement curve in all three cases.

To investigate further the influence of the flanking transmission, two of these configurations were reinforced with a layer of 150 mm glass fibre and a chip board on top of this. These measurements are shown in Appendix II.1.4 - II.1.5. We see that the chip board on 30 mm of mineral wool (II.1.4) is roughly 10 dB below the "maximum" curve up to 200 Hz, and the transmission loss measurement should be valid in this frequency region. From 250 - 800 Hz the difference is larger than 3 dB, and corrections for the flanking transmission could be made, but above that the curves are so close together that correction is not very meaningful. Regarding the case of the concrete slab on 50 mm of mineral wool, it can be said for the entire frequency region that such a correction is not meaningful.

We would still like to have some evaluation of the airborne sound insulation improvement. Reconstruction of the upper room in the laboratory was considered too expensive and it would also take too long time.

Instead we decided to make this evaluation by exciting the lower room with airborne sound and measuring the difference in acceleration level between the floating floor and the structural slab by itself. This was done for the three same configurations as shown in Appendix II.1.1-II.1.3, and the results are shown in Appendix II.2.1-II.2.3.

The acceleration levels were averaged over 10 positions and we used an accelerometer weighing 30 grams, which was fastened with bees wax. As a control for this convenient fastening method, we glued a small plate on one of the test floors and made a comparison measurement for the accelerometer fastened with wax and a screw respectively. The result is shown in Appendix II.3.2, and we can see that the difference is negligible within our entire frequency region.

So far we have only considered the flanking transmission for the 160 mm concrete slab. Do we have the same problem in the case of the 200 mm slab of lightweight concrete?

We have not made the corresponding control measurements here, but a comparison through some simple calculations may be appropriate.

Let us consider the direct transmission first. For both slabs we may assume that the resonant transmission dominates over the forced one, at least above the respective critical frequencies. The difference in velocity level between the two cases is then

$$\begin{aligned} L_{v1} - L_{vd} &\approx R_{0d} - R_{0l} + 10 \log (f_{cl}/f_{cd}) \\ &\quad + 10 \log (\eta_d/\eta_l) \\ &= 9 \text{ dB} + 10 \log (\eta_d/\eta_l) \end{aligned} \quad (3)$$

where l and d refer to lightweight and dense concrete respectively.

The loss factors for the two slabs have been measured and are shown in Appendix V.1.1-V.1.2. At high frequencies the loss factors are approximately equal or may even be slightly higher for the concrete slab. This would mean

about 10 dB higher velocity level in the lightweight slab than in the heavy slab. At low frequencies we have approximately $\eta_1 \approx 4\eta_d$ so there the difference in velocity level should only be about 3 dB.

This is also approximately the relation between the measured transmission losses of the two slabs, except that the difference is larger than predicted at high frequencies. This can be accounted for to a certain extent by using corrected bending waves.

In Appendix II.3.3 we show the calculated transmission loss for both the slabs using the corrected bending waves and the η -values in Appendix V.1.1-V.1.2. The measured R-values are shown for comparison. (For further description of the calculation method see [21]).

For the concrete slab we have rather a good agreement between measurements and theory. The deviations at the highest frequencies may indicate a little leakage. Resonant transmission dominates over the forced one even below f_c for this concrete slab.

The agreement between theory and measurements is not quite so good for the lightweight slab. Below 100 Hz and above 500 Hz the theory is seen to overestimate the transmission loss. At the highest frequencies we may here again have some leakage although considerable care was taken in the sealing work, but no explanation has been found for the deviations in the middle frequency region. At the lowest frequencies it may be that the elements in the slab radiate differently (more effectively) compared to the theoretical homogeneous slab.

Because of the high damping at low frequencies, we have here a forced (mass-) transmission below f_c , which is slightly higher than the resonant one.

Now we continue with the flanking transmission. We have found that the lightweight slab has a higher vibrational level for the direct transmission, and should therefore be less sensitive to the flanking transmission than the heavy slab. However we must also consider the indirect (flanking) transmission which is not exactly the same for the two slabs. We may use SEA and assume that we couple the slabs one at a time to an exciting system (the upper room floor) which has a certain energy, E_0 . If we now assume that each plate reaches the same acoustic temperature as this exciting system, we get:

$$\frac{E_0}{N_0} = \frac{E_d}{N_d} \quad , \quad \frac{E_0}{N_0} = \frac{E_1}{N_1} \quad (4)$$

where N stands for number of modes, or

$$\begin{aligned} L_{v1} - L_{vd} &= 20 \log (m_d/m_1) + 10 \log (f_{cd}/f_{c1}) \\ &= 5 \text{ dB} \end{aligned} \quad (5)$$

At high frequencies, where the losses in both slabs are low and approximately equal, the above assumption about equal acoustic temperature is valid. Here we have already shown for the direct transmission and simple bending wave theory that $L_{v1} - L_{vd} \approx 10$ dB. Hence the lightweight slab should have about $10 - 5 = 5$ dB extra dynamic range in the transmission loss measurements, as compared to the heavy slab.

We have already pointed out that the difference in velocity level is higher than 10 dB at high frequencies, and the dynamic range is probably increased by more than 5 dB. At the lower frequencies the 5 dB may however be considered a reasonable approximation.

We shall not dig any further into these considerations now, but we may conclude that even if the dynamic range is somewhat increased, there still will be problems in measuring the improvement of good floating floors. We have chosen to show only one measurement of ΔR on the lightweight slab. This is a 22 mm chip board on 30 mm of dense mineral wool, shown in Appendix II.1.6. In Appendix II.1.7 we show the transmission loss for this construction together with those where the interlayer is doubled and redoubled. At least in those frequency regions, where the difference to the quadrupled construction is sufficiently high, the ΔR of II.1.6 should be reliable.

5.2 Measurement of force spectra

An important factor in the impact sound insulation measurements is whether the exciting force can be considered to be the same on both the floating floor and the structural slab.

To investigate this we glued a light accelerometer (2 grams) on the top of one of the hammers in the ISO-tapping machine and measured the acceleration spectra for the hammer blow. It was found that the other hammers disturbed the measurement, if the machine was running continuously, and thus the measurements were made for only a single hammer blow.

In Appendix IV we show the results from these measurements. In each diagram we show as a reference the spectrum for the blow on the 160 mm concrete slab.

In IV.1.1 we compare the lightweight concrete slab to the dense one. Both curves are averages over a few measurements where the spreading of the results was very little in both cases. We see that both spectra are white up to a break frequency, which is about 1600 Hz for the lightweight slab and about 3150 Hz for the concrete slab ("-3 dB" frequency). The white parts of the spectra are also seen to have the same level. The surface of the lightweight concrete seems to be so much softer than the epoxy surface of the concrete slab, that the force-pulse time is approximately doubled giving about 50 % lower break frequency.

In IV.1.2 we compare our two floating floors with the 160 mm structural slab. The concrete floating slab has a slightly higher break frequency than the

structural slab. This is probably because of the epoxy surface layer, which may have reduced this frequency slightly for the structural slab. The difference at lower frequencies, where the floating slab lies 1 - 2 dB below the structural slab, is more difficult to explain. Somewhat greater spreading was experienced in the results for the floating slab than in those for the structural slab, but hardly enough to explain this difference. Anyway, the difference is relatively small, and the exciting force can be assumed to be approximately the same on both slabs in the entire frequency region.

The chip board has a very much different force spectrum. Firstly it falls off already at 500 - 630 Hz, and secondly its white part lies almost 6 dB below the one for the structural slab. In IV.1.3 we take a closer look at these two spectra and calculate the difference spectrum. Here it is probably not the softness of the chip board that gives this very low break frequency. Instead it is the phenomenon mentioned in part 2.1, that the mass impedance of the hammer becomes large compared to the point impedance of the board. We may call the frequency where the two impedances are equal f_2 :

$$\begin{aligned} f_2 &= 8\sqrt{MB} / (2\pi M_0) \\ &= 8\sqrt{MB} / \pi / \text{kg} \end{aligned} \quad (1)$$

This frequency has the following numerical value for our test objects:

	f_2
160 mm concrete slab	160 kHz
200 mm lightweight slab	130 kHz
50 mm concrete slab	16 kHz
22 mm chip board	550 Hz

We see that the measured break frequency for the lightweight concrete slab lies far below f_2 . However, the measured break frequency for the chip board agrees well with this frequency f_2 .

Why is there a 6 dB difference at low frequencies? Lindblad [39] has shown that if the hammer does not rebound when it strikes the slab, we get a 6 dB improvement, compared to the case where the hammer hits a hard slab and rebounds without losses. Lindblad deduces the condition for no rebound as:

$$\sqrt{KM_0} > 2 \cdot Z_M \quad (2)$$

where K is the compliance of the slab, M_0 is the mass of the striking hammer and Z_M is the point impedance of the slab. If we assume that K is dominated here by the spring stiffness of the interlayer and introduce $Z_M = 8\sqrt{mB}$ and $M_0 = 0.5$ kg, we can write the condition (2) as:

$$\eta_0 = \frac{1}{8} \sqrt{\frac{0.5D_s/h_2}{mB}} > 2 \quad (3)$$

For our configuration in IV.1.3 we get $\eta_0 \approx 1.15$, which corresponds to a small rebound. In [39] the case of $\eta_0 = 1$ is shown to correspond to about 5 dB lower level than a perfect rebound without losses would give. This seems to agree rather well with our result in IV.1.3.

We still have one thing left to point out in IV.1.3. The correction term for the low point impedance of the chip board is (see [6] e.g.):

$$\Delta F = 10 \log (1 + (f/f_2)^2) \text{ dB} \quad (4)$$

However, the difference spectrum in IV.1.3 rises more steeply, suggesting that the break frequency, because of the softness of the surface, also comes into play. The "3 dB" frequency after correction by (4) is about 1000 Hz, which then should be this break frequency.

In IV.1.4 the chip board is measured on top of the relatively heavy concrete tiles, which by themselves have a high break frequency. Here we find a break frequency for the chip board as a "surface layer" about 800 Hz, and then the curve falls off very steeply. This extremely steep slope has not been explained, but we may point out, that the chip board lay loose on the tiles, and this may have influenced the result.

Finally in IV.1.4 we show the force level for the concrete tiles by themselves, which were as small as 350 mm x 350 mm. Here a considerable difference was found, depending on whether the hammer struck the tile near the middle, near an edge or near a corner. In practice the tapping machine was placed so that all these striking positions were used, so we have in IV.1.4 averaged over two middle positions, two edge positions and two corner positions. We get an average curve very similar to that of the homogeneous 50 mm slab, with a white part somewhat lower than for the 160 mm reference slab, and a slightly higher break frequency.

In IV.1.5 we show the measurement results for the three different striking positions on a tile. The

corner position has the lowest white spectra and the lowest break frequency. This may be caused by a reduced point impedance for the tile at the corner, giving similar effects as previously discussed for the chip board.

Summing up we should be able to assume approximately the same white force spectra for the 160 mm structural slab, the 50 mm floating slab and the concrete tiles. Corrections should be made for the chip board and perhaps also for the lightweight structural slab at high frequencies.

5.3 Loss factors of the different floors and slabs

An important parameter in the theoretical work is the degree of damping of the different floors and slabs. We have measured this for several configurations. The method we used was to fasten an accelerometer to the test object, excite it with a hammer blow and register the resonances or modes with the help of a FFT analyzer. Then the resonance frequency (f_N) and the half-value bandwidth (b) was measured, and the loss factor calculated as:

$$\eta(f_N) = b/f_N \quad (1)$$

Several measurements (30 - 40) at different frequencies were made for each object and the results plotted on a log-log diagram. Then the mean square line was calculated where it seemed reasonable, and from this the values at different 1/3-octave frequencies were calculated.

In Appendix V.1.1-V.1.2 we show the results for the two structural slabs, without any floating constructions. The loss factor for the lightweight slab seems to decrease with frequency in such a way that the mean square line can be considered a reasonably good approximation.

The loss factor for the structural slab behaves a little differently. At low and middle frequencies it decreases in the same way as for the lightweight slab, but has a lower numerical value (4 - 5 times lower). At high frequencies the loss factor approaches a constant value, approximately $\eta \approx 0.014$. We have divided the measurement results into two groups, one for $f_N \leq 500$ Hz, and the other for $500 < f_N < 5000$ Hz. For each group we calculate a mean square line as shown in V.1.2. The high frequency line has a slight incline

but if we choose the division point at a slightly higher frequency we get an almost horizontal line.

The horizontal line probably represents the material loss factor, whereas the frequency dependent line at lower frequencies includes "edge absorption", which is the energy lost to other construction elements. The lightweight concrete should have a material loss factor approximately of the same numerical value as the one for the dense concrete. It would probably have emerged in figure V.1.1, if some more measurements had been made at the high frequency end.

Now, why does the lightweight slab have a much higher "edge absorption" than the heavy slab?

We can only say that the edge conditions were not exactly identical for the two slabs. The lightweight concrete elements were more or less in direct contact with the floor of the upper measurement chamber (the vertical sides of the test opening). The dense concrete slab on the other hand had almost no contact, except through the rubber foundation strip. This may be a sufficient explanation.

Now we consider the floating floors. These were measured "in place", i.e. resting on the elastic interlayer. As previously mentioned in part 4.4 this leads to a radiation loading which is added to the inner (material) loss factor. No "edge absorption" has any influence here, as the edges are free. We would expect the largest influence from the radiation loading on the relatively light chip board, as the total loss factor can be written as:

$$\eta = \eta_1 + \left(\frac{\rho_s c_s}{\omega m_1} \right) \quad (2)$$

In V.1.3 we show η for the chip board resting on dense mineral wool with $\rho_s = 160 \text{ kg/m}^3$. According to part 5.4 the mineral wool should have $D_s \approx 0.26 \text{ MPa}$, when loaded with a single chip board (0.15 kPa static load). This D_s corresponds to $c_s = \sqrt{D_s/\rho_s} \approx 40 \text{ m/s}$ and (2) gives much too high values of η compared to the measurements.

At frequencies above approx. 1000 Hz we have measured $\eta \approx 0.03$ according to V.1.3, and this may be interpreted as η_1 , the material loss factor. With $m_1 = 15 \text{ kg/m}^2$ in equation (2), we get the best agreement with the measured values, if we set $\rho_s c_s = 2200 \text{ kg/sm}^2$.

No explanation has been found for this low "effective" wave impedance $\rho_s c_s$, but we may point out that the value for D_s in 5.4 is found by extrapolation, and it is perhaps not very exact. It is also possible that we should only take the stiffness of the mineral wool structure itself, and not the combined stiffness of the entrapped air and the structure, which D_s represents. (Suggested by Nilsson in [10].) These explanations are however not sufficient to obtain the extremely low value for $\rho_s c_s$, which is necessary for (2) to agree with the measured results.

Some further investigations would be interesting here, e.g. the measurement of η for different qualities of the mineral wool, and perhaps also for different loading conditions.

However, we only measured η for the one configuration of chip board floor shown in V.1.3, but we can at least state that η is greatly increased at the lower frequencies, probably because of radiation loading.

We have however made a series of measurements on the 50 mm floating slab on the three different types of mineral wool: A, B, C. These measurements are shown

in V.1.4-V.1.6. We also wanted a measurement without the radiation loading of the mineral wool, so we lifted the slab and measured η for it hanging from a crane. We may have introduced some new losses through the crane, but the result seems to be trustworthy. It is shown in V.1.7, and we have approximately a frequency independent $\eta \approx 0.010$.

In figure V.1.8 we show all of the four mean square lines together. The line for the heavy mineral wool A lies highest as could be expected, because it has the highest $\rho_s c_s$. The same reasoning should give a much lower line for the light glass fibre, C, than both A and B. Instead the line for C is found to be almost identical to the line for B. Also here we get far too high values for η if we use equation (2) with D_s from part 5.4. For mineral wool A as an example, we get $D_s \approx 0.55$ MPa (1.2 kPa static load) corresponding to $c_s = 59$ m/s and $\rho_s c_s = 9400$ kg/sm². (This gives $\eta \approx 0.13$ at 100 Hz and $\eta = 0.02$ at 1000 Hz.)

It seems evident that we cannot use equation (2) without some modifications.

Finally in V.1.8 we show the measured η values for a floor of concrete tiles 350 x 350 mm. The first modes were registered at about 1000 Hz, and above this frequency, η seems to be constant, approximately 0.014. Below 1000 Hz we cannot measure the losses with the method used here.

In [40] Leissa publishes tables for calculation of the first flexural modes of rectangular plates with different edge conditions and three different edge length relations. The first mode for a 350 x 350 x 50 mm concrete slab with all its edges free is $f_{22} = 870$ Hz according to Leissa, and the next ones are $f_{13} = 1280$ Hz

and $f_{31} = 1576$ Hz. For the concrete tile simply supported at all edges we have the first mode $f_{11} = 1270$ Hz. The tiles are in contact with each other, and transverse movement may be prevented to some extent along the edges, which could mean a slightly higher resonance frequency for the first mode than the theoretical value for the case where all the edges are free.

5.4 Dynamic modulus and wave velocity of the inter-layer

In [30] we measured the dynamic modulus of dense mineral wool with different static loading. This was done simply by measuring the change in resonance frequency for larger and larger masses on top of the mineral wool. The result showed that the mineral wool was very unlinear with an increasing dynamic (and static) modulus with increasing static load. The dynamic modulus as a function of the load had principally the shape shown in the figure below:

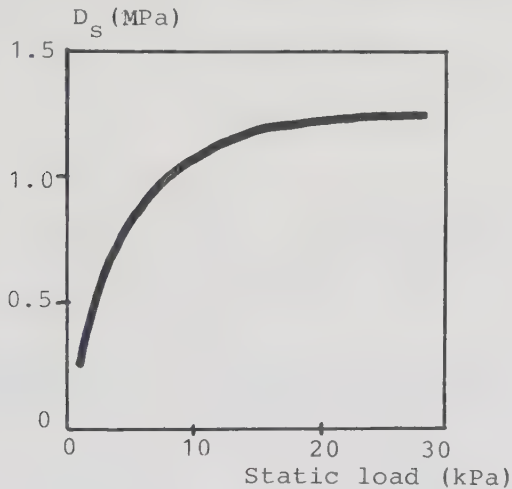


Figure 5.4.1. Principal variation of the dynamic modulus as function of static load for a dense mineral wool (160 kg/m^3). (The influence of entrapped air excluded.)

Now in the case of floating floors we have a static load from the mass of the floor approximately equal to 1.2 kPa for a 50 mm concrete slab and 0.15 kPa for the light chip board. To this we may add the "live load" on the floors.

According to [41] typical live loads in apartment buildings are 0.2 kPa, and in office buildings 0.5 kPa. Considering all this, we see that we are confined to the steep end of the curve in figure 5.4.1 for most practical floating floors. Relatively small variations in the static load may influence the dynamic modulus considerably, and thus also the sound insulation properties of the floating floor.

In the ISO standard [37], part VIII which considers the impact level measurements of light floating floors, there is recommended an extra load of 1 kPa on such light floors as our single chip board. As pointed out by Bodlund in [42] this seems to be a rather high value, considering the above mentioned values from [41].

We made a series of impact level measurements, where the load was varied in four steps. First we measured the chip board floor with no extra loading. The second measurement was made with a 0.15 kPa extra load, the third with a 0.45 kPa extra load and finally the fourth with the extra load of 1.0 kPa recommended in [37].

These load steps correspond to a load on the mineral wool from a single, double and quadruple chip board and finally a 50 mm concrete slab.

The placing of the extra loads was made in accordance with the recommendation in [37] (part VIII, Figure 1). One heavy mass is placed along each long side, resting on four legs, which are about 2 m x 0.5 m apart. Two smaller masses are then placed asymmetrically near each short side.

The result is shown as the impact sound insulation improvement for the four load cases in Appendix VI.1.1. We can see that the difference is considerable, about 6 - 8 dB between the highest and the lowest curve, and 2 - 3 dB for each of the load steps. At high frequencies this difference is however reduced.

In [42] a measurement is shown, where a very light floor (6.4 mm fibre board) on 20 mm of 250 kg/m³ mineral wool was measured without a load and with an extra load of 0.2 kPa. The result is different from ours, no change was found at low frequencies, but approximately 4 - 5 dB at high frequencies. The measurement was repeated after a conditioning period of 6 days with the load, and only a slight change was found, again mainly at high frequencies.

We also made an extra measurement after a conditioning period of 10 days. The result is shown in Appendix VI.1.2 and we see that we only have marginal changes, approximately 0.5 dB at low frequencies.

The result in VI.1.1 confirms our statement that relatively small changes in the static load on the mineral wool may change the sound insulation properties of the floating floor considerably.

Let us assume that the extra load on the chip board has little influence on the properties of the board itself, but mainly increases the stiffness of the interlayer. The approximate 8 dB change at low frequencies, where the forced solution is probably valid, then corresponds to a change in f_1 of about $10^{8/40} = 1.6$. This in turn implies a change in the dynamic modulus by a factor $1.6^2 = 2.5$ when the static load is increased from 0.15 kPa to 1.2 kPa.

In [43] the unlinear behaviour of the mineral wool is pointed out, referring to [33] and [44]. It is furthermore suggested that a new test method for the Nordic countries (Nordtest) for the measurement of the dynamic modulus of mineral wool should include measurements at three different static loads; 1, 2 and 4 kPa. It is also pointed out that very few measurements of the dynamic modulus of mineral wool can be found in the literature. Only three references were found: [36] where only one value of the static load is used, [33] where Kovrigin tests the long time variation (up to 30 days) of the dynamic modulus with several different static loadings, and [44] where the dynamic modulus is measured for three different values of static load.

We have found two more references of a more recent date: [45] and [27]. In [45] a large measurement series for Rockwool of two different densities is presented. Here the measurements have been made for static load up to 30 kPa and the results agree in principle with the curve shown in our figure 5.4.1. The series includes measurements at 1, 2 and 4 kPa for both qualities of the Rockwool, 160 kg/m³ and 100 kg/m³.

Results are published in [27] for these same two qualities of Rockwool and three qualities of glass fibre, all measured at 0.5, 1, 2 and 4 kPa static load.

Below we show a diagram where we have borrowed the four measurement values for each curve from [27] and inter- and extrapolated the continuous curves from those.

The diagram has log-log scales, as it was considered

easier to extrapolate the curves in such a diagram than in a lin-lin diagram.

Three curves are drawn, corresponding to our three types of interlayer A, B, C.

We have also drawn four vertical lines corresponding to the single, double and quadruple chip board and the 50 mm concrete slab.

For comparison we also show values from [45] and [30].

Finally we have drawn an approximate curve for the interlayer d, a sponge-like foam plastic, on the basis of two resonance measurements for 1.2 and 4.2 kPa static load.

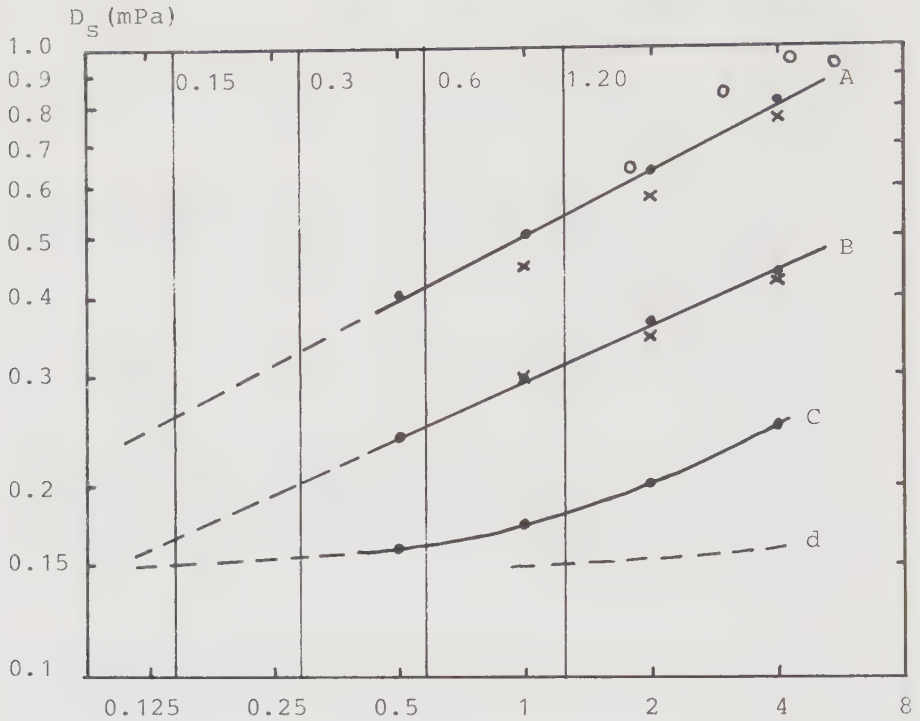


Figure 5.4.2. Dynamic stiffness of the three different interlayer materials A, B, C, including the dynamic modulus of the entrapped air (10^5 Pa)

• From [27]

× From [45]

◦ From [30]

The curve for d is drawn on the basis of only two measurements.

We may note that the extrapolated value for the 0.15 kPa static load on A yields approximately 0.26 Mpa, whereas the value for 1.2 kPa static load is approximately 0.55 MPa. The modulus thus increases by a factor 2.1 which agrees reasonably well with the value 2.5 deduced from the curves in Appendix IV.1.1.

Now the question may be raised about the variation of the dynamic modulus with the frequency. Usually it is assumed that it does not vary with the frequency, but very little is in fact known about this. The modulus is determined with resonance measurements on a mass-spring system. The resonance frequencies are as low as 15 - 50 Hz approximately, and usually the measurements are made with an "open sample", so the air can be assumed to flow sideways, and not influence the measured spring stiffness.

In [46] Kraak discusses the influence of the air stiffness on such resonance measurements. We take as an example a test area of 350 x 350 mm and estimate the specific flow resistances, r , for our three types of interlayer, A, B, C. Then we may, according to Kraak, calculate two limiting frequencies for each material f_{r_1} and f_{r_2} :

	r Ns/m ⁴	f_{r_1} (Hz)	f_{r_2} (Hz)
A	$5 \cdot 10^4$	25	2000
B	$4 \cdot 10^4$	30	3000
C	$3 \cdot 10^4$	40	4000

Table 5.4.1.

Below f_{r_1} we measure only the stiffness of the structure and above f_{r_2} we measure the combined stiffness of the structure and the air.

We see that the measurement region of 15 - 50 Hz lies very close to the frequency f_{r_1} and we may conclude that the air does not influence the measured spring stiffness.

In [45] measurements are made with open and sealed sides of the test material respectively. The results show a difference of 10^5 Pa in the dynamic stiffness, which is the isothermal modulus for air. It seems as though we can add the measured stiffness and the isothermal modulus of air to get the combined stiffness. This has been done in figure 5.4.2, so D_s represents this combined stiffness.

We still have to consider the fact that D_s is only known at low frequencies.

In order to investigate the frequency dependence of the dynamic modulus, we tried a new type of measurement. Instead of measuring the resonance frequency for a spring-mass system, we tried to measure the wave speed, c_s in the mineral wool. This was done with the help of a two channel FFT-analyzer, which can measure the phase transfer function between two transducers. As transducers we used a pair of very light accelerometers (2 grams), fastened on thin metal plates, which were placed at different heights in a heap of the mineral wool to be tested.

The measurement arrangement is shown schematically in figure 5.4.3 below.

Below the second plate we let the mineral wool continue for approximately 1 m to ensure that reflections from the concrete floor do not disturb the measurement. (Approximately 30 - 40 dB damping of the pulse was measured along the measurement distance of 200 - 400 mm.)

As a source we tried a vibrator fed with PN-noise (pseudo random noise) from the FFT-analyzer, but this did not give satisfactory results. Instead we

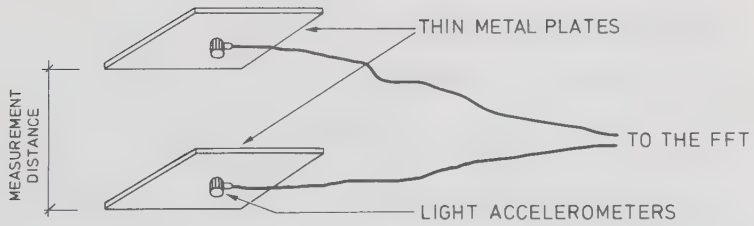
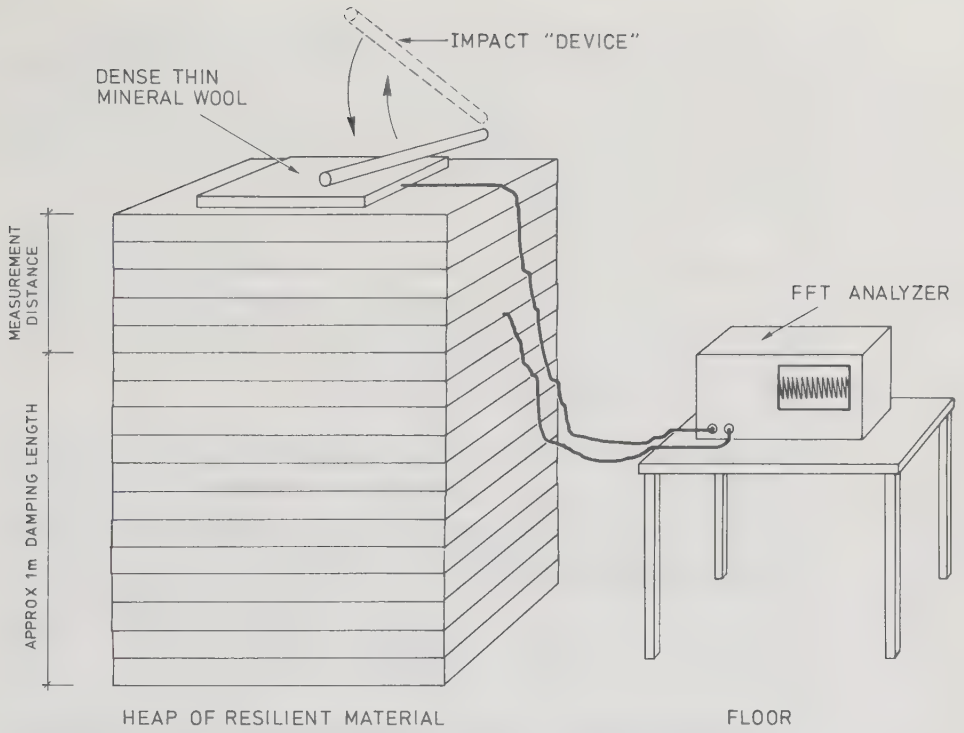


Figure 5.4.3. Schematic arrangement for the measurement of the phase velocity c_s .

went over to excitement with mechanical blows. As "impact device" we tried pieces of wood, metal bars etc. It was found that devices with a small contact area gave results different from those with a large contact area (boards.)

It was found necessary to have the first transducer close to the excitement place, and we got the best results when only a thin layer of dense mineral wool covered the upper accelerometer.

From the excitement point, we assume that a spherical wave propagates down through the mineral wool. At the distance r_1 from the source we measure the phase of the propagating wave:

$$v(r_1) = v_0 \frac{r_0}{r_1} \left(1 + \frac{1}{jk_S r_1}\right) e^{-jk_S(r_1 - r_0)} \quad (1)$$

At the distance r_2 from the source we also measure the phase of the wave:

$$v(r_2) = v_0 \frac{r_0}{r_2} \left(1 + \frac{1}{jk_S r_2}\right) e^{-jk_S(r_2 - r_0)} \quad (2)$$

The difference in phase is then:

$$\begin{aligned} \Delta\varphi &= \arg \{v(r_1)/v(r_2)\} \\ &= k_S(r_2 - r_1) + \arctan(1/k_S r_2) - \arctan(1/k_S r_1) \end{aligned} \quad (3)$$

If $k_S r \gg 1$ we have

$$\Delta\varphi \approx k_S(r_2 - r_1) \quad (4)$$

Below we show a calculation of $\Delta\varphi$ according to (3) for a fictitious example of $r_1 = 20$ mm, $r_2 = 220$ mm and the wave velocity $c_S = 40$ m/s independent of the frequency.

The result is plotted in the figure below.

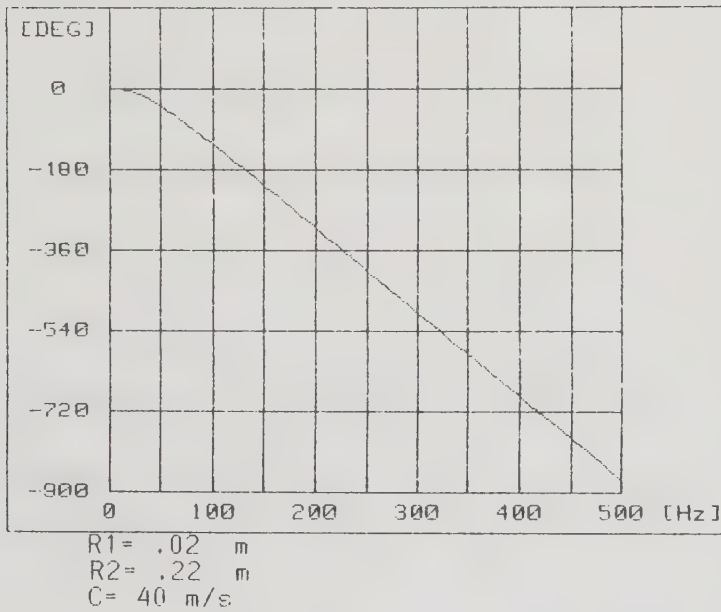


Figure 5.4.4. Theoretical frequency variation of $\Delta\phi$ in (3) for a frequency independent velocity $c_s = 40 \text{ m/s}$.

We see that above approximately 100 Hz ($k_s r \approx 3$) we can use the approximation (4) and the phase velocity can be calculated as:

$$c_s = 2\pi(r_2 - r_1)/(d\Delta\phi/df) \quad (5)$$

The FFT-analyzer only displays the phase in the interval $\pm 200^\circ$, and if the phase exceeds these limits the curve jumps 360° . A curve like the one in fig. 5.4.4 is thus displayed as a saw-tooth pattern. Such patterns were indeed measured in practice, and we started the evaluation by "rolling up" the phase curve. Then we found a running mean square average of the slope along the curve, taking the average over 13 - 25 frequency points, and calculated and displayed the

phase velocity according to (5). Several examples are shown in Appendix VI.2.1- VI.2.7.

At middle and high frequencies we get a rather smooth and horizontal curve for c_s . In this frequency region we calculate a mean square value for all the frequency points within it. This average velocity is printed and a line is drawn to show the value and the frequency region over which it is calculated.

We may note that we have some irregularities at low frequencies. The phase curve has a less steep slope there, which may indicate a higher velocity. We see, however, in our theoretical curve in figure 5.4.4 that the slope gradient indeed decreases even for frequency invariant c_s . Calculating c_s according to (5) would then lead to higher apparent c_s at lower frequencies (below 100 Hz approximately). In our measurements we find that this higher apparent c_s sometimes continues to slightly higher frequencies (200 - 300 Hz). Some measurements even show a clear break-point with higher velocity at low frequencies and lower velocity at high frequencies.

The reproducibility of this phenomenon is, however, not quite satisfactory, and we have also noted that decreasing the measurement distance moves this break-point to a higher frequency. Our original hypothesis about a physically different wave velocity at low and high frequencies respectively has thus been rejected.

It was thought, that at low frequencies we might have a combined structural wave and air wave, and that at higher frequencies the air wave would be so heavily damped, that only the structural wave would remain.

In [47] Zwikker and Kosten deduce an equation for these two waves, coupled by the flow resistance in the mineral wool. (Similar to the coupled bending waves in (2.1.3).) It would be interesting to make numerical calculations on the basis of these coupled equations, and see what wave velocities this would lead to. This has however not (yet) been done.

One more thing that contradicts our previously mentioned hypothesis is a measurement series with different impact devices, where we hit the mineral wool with broader and broader pieces of wood. When we use a narrow 10 mm or 25 mm contact area, we may probably assume that only a structural wave is excited in the mineral wool.

When broader pieces (up to 290 mm) are used, we notice an increase in the measured velocity. This may be interpreted so that we now also excite the air wave. The modulus is then increased by 10^5 Pa and the velocity increases in accordance to that. The increase in velocity is experienced over the entire frequency region. (See Appendix VI.3.1-VI.3.2.)

To investigate this further we tried measuring with perforated metal plates, thus trying to separate the two types of waves. When a narrow impact device was used, there was no difference between the results with tight and perforated plates respectively. When the broader impact pieces were used, we measured a slightly lower velocity with the perforated plates than with the tight ones. The measured curves were however more irregular and did not reach the low values measured for the narrow impact devices. (Compare as an example VI.2.4, VI.2.6 and VI.2.7.)

The perforated 0.5 mm plates had ϕ 3 mm holes at c/c 7 mm, which corresponds to approximately 15 % open area. If this plate is used in front of a porous absorbent in a room, α_0 is reduced below 0.8 only for frequencies above approximately 3000 Hz.

Several types of accelerometer plates were tested. It was assumed at first that ω_m for the plate should not exceed $\rho_s c_s$ of the material if the measurement was to be considered valid. For a 2 mm aluminium plate we have $m = 5.4 \text{ kg/m}^2$, and $\rho_s c_s$ for a heavy mineral wool was assumed to be close to $160 \times 50 = 8000 \text{ kg/(sm}^2\text{)}$. This would mean an upper limiting frequency $f_m \approx 250 \text{ Hz}$ for our 2 mm aluminium plate, or even lower if we wanted $\omega_m \ll \rho_s c_s$.

In Appendix VI.4.1 we show a measurement with this 2 mm plate, and we see that the results seem to be all right up to about 600 - 700 Hz. In order to extend the frequency region, we went over to use only 0.5 mm aluminium plates, which then should give a four times higher limiting frequency. As an example we can take the curve in VI.2.2, which shows reasonable results up to about 1700 Hz, or VI.2.5 up to 2000 Hz. The numerical values for c_s were found to be very similar for the two different plates, but a more smooth curve was usually achieved with the thin plate.

We also tried different sizes of the thin plate and got the same result for a 350 x 350 mm plate as for a 70 x 70 mm plate, so we continued to use the more convenient small plate.

The weight of the accelerometers was chosen as small as possible. Usually, when measuring bending waves in a plate we must make sure that

$$\omega M_a < Z_M \quad (6)$$

where M_a is the mass of the accelerometer, and Z_M the point impedance for the plate ($8\sqrt{MB}$). Our 0.5 mm aluminium plate has $Z_M \approx 23 \text{ Ns/m}$, giving $f < 1900 \text{ Hz}$.

However it is not the bending waves we are measuring here, and the condition (6) may not apply directly here. Still a heavy accelerometer would probably disturb the measurements, and as light accelerometers as possible should be used.

Below is a table showing the average measurement results for c_s and the corresponding values of $D_s = \rho_s c_s^2$. Two values are shown for each material, one where the impact device is narrow ($b = 2.5 \text{ mm}$) and should only excite the structural wave, and another for a broad device ($b = 290 \text{ mm}$) where both the structural wave and the air wave should be excited. Some measurement examples are shown in Appendix VI.2.1-9. Table 5.4.2. Average results for velocity measurements.

		b = 25 mm		b = 290 mm	
	measurement distance (mm)	c_s (m/s)	D_s (MPa)	c_s (m/s)	D_s (MPa)
A	400	49	0.38	55	0.48
B	300	35	0.12	50	0.24
C	200	22	0.02	-	-
a	300	37	0.22	45	0.32
d	300	21	0.05	26	0.07
g	310	25	0.10	30	0.14

It is interesting to note the very large difference between type A and g ("unwalked and walked" mineral wool). We may also note the considerable

difference between A and "a", which we would expect to give identical results, as the density is the same. C and d show an extremely low structural modulus.

In the case of A, B and "a" the difference between the two columns is approximately 10^5 Pa, the isothermal modulus of air. However, we measured a lower difference for g and d, and no valid results were measured for C with the broader contact area.

If we compare the values in the table 5.4.2 to the values in figure 5.4.2, we see that the value for A corresponds to a static load of about 0.9 kPa. The value for B corresponds to a static load of about 0.5 kPa. We have, of course, a static load in the phase velocity measurements from the weight of the mineral wool itself, and this static load increases along the measurement distance. For A we have the load 0.64 kPa at the lower plate (400 mm), and for B this is 0.3 kPa (at 300 mm).

The materials d and C have a lower modulus here than in figure 5.4.2, and the modulus of "a" is lower than expected.

The unlinear behaviour of the mineral wool may be explained in two different ways. Firstly it may be considered to be a surface phenomenon. More and more static load leads to a better contact with a larger area of the structure of the material. Figure 5.4.1 might imply such a surface phenomenon, as the modulus becomes more and more linear with increasing load. Secondly the unlinearity may be considered as a physical change of the modulus itself as a material property with an increasing static load.

If the unlinearity is a pure surface phenomenon we would expect to measure wave velocities corresponding

to the high load asymptote in figure 5.4.1 ($D_s \approx 1.2$ MPa for the mineral wool A). This is not the case, we measure instead values corresponding approximately to the static loads caused by the weight of the mineral wool itself.

The conclusion must be that we indeed have a new physical modulus for the mineral wool, perhaps combined with the above mentioned surface phenomenon for low static loads.

Finally we can state that our new measurement method may help us in explaining the properties of the mineral wool. It seems that we can assume the velocity c_s to be frequency independent, even though a slight decrease with increasing frequency may be noted for some of the measurements. It also seems that we can add the isothermal modulus of air to the structural modulus, even at high frequencies.

However it seems more safe to use values from the traditional resonance measurements in calculations of the sound insulation properties of floating floors. In these measurements we have a better control over the unlinear behaviour of the mineral wool.

We have made several attempts to determine the loss factors of the different interlayers at higher frequencies, using resonance methods. These attempts have so far been unsuccessful and we only have the values from the traditional method of measuring the dynamic modulus, where the resonance bandwidth can also be measured and η calculated.

These values vary more in the different references than the values for the dynamic modulus, and η is also reported to vary with different static loads.

Nothing is known about the frequency variation of η .

[36] reports $\eta = 0.23$ for A and $\eta = 0.28$ for B. [45] reports approximately identical values in the case of a 1 kPa static load, but considerably lower values for larger static loads. [44] reports approximately double values (0.5 - 0.6) for both rock-wool and glass fibre. Our own measurements show $\eta \approx 0.10 - 0.15$ for A with relatively large static loads (4 - 6 kPa).

The loss factor of the interlayer seems to be a parameter which is difficult to determine precisely.

In our calculations we choose $\eta_2 = 0.2$ and leave the frequency dependance as an open question. The measurement results of the impact sound level compared to calculations imply that η_2 may decrease with increasing frequency, otherwise we get a too high damping term at high frequencies. As η_2 is an important parameter in these calculations, it would obviously be an advantage to know with a better precision, both the numerical value at low frequencies and the frequency dependence.

6. DISCUSSION OF THE RESULTS

6.1 The influence of different parameters

We now wish to discuss and to compare our results from the sound insulation measurements. We try to take one parameter at a time and study its influence. The curves to be compared are taken from the Appendixes I and II and the improvement curves are plotted out again in pairs to make the comparison more easy. These comparison curves are shown in Appendix III.

In almost all our comparisons we use the normalized impact level to judge the sound insulation properties. In 6.1.8 we compare this to the most reliable measurements of airborne sound insulation.

6.1.1 The width of the interlayer

We made two measurement series to study the influence of the width of the interlayer. In both cases we used the mineral wool of type "a" as an interlayer, and measurements were made for 30, 60 and 120 mm widths.

In Appendix III.1.1 we show the three improvement curves for the 50 mm concrete slab as a floating floor. At low frequencies we may assume the forced solution to be valid, and according to this, the improvement should increase by $40 \log(\sqrt{2}) = 6$ dB, when the width of the interlayer is doubled. This improvement can indeed be seen when the width is increased from 30 to 60 mm. These two curves seem to follow the forced solution up to about 200 Hz. Above that we have a different slope, probably mainly because the interlayer starts behaving like a wave

medium. Resonant transmission also has some influence. At the intermediate frequency region the curves are approximately equal, but at the highest frequencies the curves for the wider interlayers increase more quickly.

All this seems to agree rather well with our theoretical considerations in part 4.2 and 4.3. We may point out that the forced solution of curve (3) seems to be valid only up to about 100 Hz, and thus is difficult to spot. We may also note that the valley at the fundamental resonance becomes more shallow for increasing widths.

Let us now consider the results shown in III.2.1, where the same measurement series is shown for a light floating floor, the 22 mm chip board. At a first glance we may think that the results are very much different from the ones in figure III.1.1. We might be tempted to say that the curves follow the forced solution rather well in the entire frequency region, and that the increase in ΔL from doubling the width is not 6 dB but closer to 3 dB.

If we take a closer look, our two cases may not be so different after all. We must remember the influence of the different force spectra for the chip board and the lightweight concrete. These were measured in Appendix IV for the lightweight concrete and configuration (1). Configurations (2) and (3) should have a similar spectra as (1), with maybe a slightly higher white part (lower spring stiffness). After this correction we will seemingly have a similar picture as in III.1.1.

We must remember that the modulus of the interlayer and the mass of the floating floor is lower than in III.1.1, and that the resonant transmission can

probably be neglected for the chip board.

The forced solution for (1) and (2) are probably valid up to about 250 Hz. The difference is not 6 dB however, rather 3 - 4 dB. Above 250 Hz the lower slope of the wave layer solution takes over, but this is partly hidden by the difference in force spectra. In Appendix III.8.1 we show the curve (1) corrected for the difference in force spectra, and there we can see the two different slopes with a break point at about 250 Hz. In the intermediate frequency range, we have relatively small differences between the three curves, but at high frequencies this difference increases again. After the force correction curve (3) will have the character of the wave medium solution over the entire frequency region, just as curve (3) in III.1.1.

6.1.2 Different materials in a constant width interlayer

Here we keep the width of the interlayer fixed, and compare different materials used as interlayers.

At first we show the previously mentioned comparison between "walked" and "unwalked" mineral wool of type A. This is done in Appendix III.2.1. We may state that the difference is not very extreme, but still significant, and it would have disturbed our comparison study if this phenomenon had not been noticed.

In III.2.2 we study the three different types of mineral wool, A, B and C. The slabs are the 160 mm and the 50 mm concrete ones. We may firstly point out that the curves all have a very similar form. Again this form corresponds in principle to the theoretical one

discussed in part 4.2.

If we assume that the loss factors of the different materials are approximately equal and have the same frequency dependence, we have only one parameter left and that is the wave impedance of the interlayer $\rho_s c_s$. (Maybe we should say two parameters, ρ_s and c_s .) Our assumption above about the loss factors is supported by the very fact that the curves all have almost exactly the same form.

From figure 5.4.2 we read the dynamic modulus $D_s = 0.55, 0.32, 0.17$ MPa for A, B, C respectively. For the forced solution this corresponds to a difference of about 10 dB between A and C and about 5 dB between A and B.

For the "wave medium solution" we have the difference of $20 \log ((\rho_s c_s)_A / (\rho_s c_s)_B)$ and considering $D_s = \rho_s c_s$ we get a difference of about 10 dB between A and C, and about 4 dB between A and B.

These numerical values for the differences seem to agree rather well on the average for curves A and B. The difference to C is however larger than the estimated 10 dB. 12 - 14 dB seem to be more correct. The modulus of C is already close to the isothermal modulus of air and cannot be assumed to have a much lower value. If it is lowered to 0.12 MPa, the difference between A and C becomes 12 dB at high frequencies and 13 dB for the forced solution. This seems to be more in agreement with the measurements shown in III.2.2. It may be pointed out, that our measurement of the wave velocity indicated a very low structural modulus for C, and a total modulus of approximately 0.12 MPa.

We also consider these three materials as interlayers for the chip board. This is shown in III.2.3. Here we have again to consider the effect of the reduced force on the chip board as compared to the concrete. As discussed in 6.1.1 we can assume the "wave medium solution" to be valid for the chip board too, but its slope is partly hidden by the effect of the reduced force. Here the wave medium solution seems to take over at about 200 Hz.

The measured difference between A and B is approximately 2 - 3 dB at low frequencies and between A and C approximately 6 - 7 dB. At higher frequencies these differences are approximately 4 and 8 dB respectively (except at the highest frequencies, where C and B have almost the same values).

Again we consult our figure 5.4.2 and read the dynamic modules $D_s = 0.26, 0.17$ MPa for A and B. To be consequent we must change the modulus of C to 0.12 MPa.

For the forced solution this corresponds to a difference about 4 dB between A and B and 7 dB between A and C. At higher frequencies about 4 dB between A and B and 8 dB between A and C. This means that by modifying the modulus of C from figure 5.4.2 to being linear with the value 0.12 MPa (up to a static load of 1.2 kPa), we now have what seems to be a good agreement between our principal model and the measurements. Direct numerical evaluation is shown in part 6.2 below.

As a final example here, we may take the interlayers A and B with the concrete tiles as a floating floor. This is shown in III.2.4. Here we have the same modules as for the 50 mm concrete slab, and the result is also rather similar. We have a higher difference here at the fundamental resonance, about 10 dB, but

above that we have about 5 dB as before, and closer to 4 dB at high frequencies.

6.1.3 Different materials and different widths of the interlayer

We would also like to consider our "extra" interlayer, the foam-plastic, type "d". We have no other interlayer of the same width to compare it to, so we must change one more parameter in our comparison here. As a reference we have chosen the 50 mm interlayer of A.

In III.3.1 and III.3.2 we show the improvement curves for these two interlayers, first with the concrete slab as a floating floor and then with the chip board.

Let us start with III.3.1. We may at first find it rather surprising, that the thin layer of "d" gives better insulation improvement than the 2.5 times thicker layer of A. The modules of the two are, however, even more different than that. Consulting figure 5.4.2 gives $D_g = 0.55$ and 0.15 MPa respectively. In the same way as in part 6.1.2 we may thus expect a difference at low frequencies of about $40 \log (\sqrt{0.55/0.15} / \sqrt{5/2}) = 3$ dB between the two. We have a slightly higher measured difference in III.3.1, but still an acceptable agreement. We may point out that the curve form for interlayer "d" also in principle agrees with our theoretical curve form, as we have previously pointed out for the curves A, B, C in III.2.2.

However, there are some deviations from the theory when we compare the two curves in III.3.1. We would expect the forced solution to continue higher up in frequency for d than for A. The first higher order

resonance, $f_n = nc_s/(2h_2)$, occurs at about 900 Hz for "d" compared to about 600 Hz for A. Both curves still have a measured break point at 200 Hz, and we may also point out that all the curves in III.2.2 had this same break point. We might be tempted to blame this phenomenon on the resonant transmission, but according to our theory in part 3, this should not be noticed below $f_{c_1} \approx 380$ Hz.

Anyway we have approximately the expected difference $20 \log ((\rho c_A)/(\rho c_d)) = 7$ dB between the two curves in the intermediate frequency region. At the highest frequency region we would however have expected the opposite effect to the one in III.3.1. Here the damping term comes in, and it is directly proportional to the width of the interlayer and the loss factor and inversely proportional to the wave velocity, c_s . (See equation 4.1.3.) If the loss factors were equal for the two interlayers, then the damping term for A would be approximately 50 % higher, and the difference between the curves would decrease at high frequencies. Instead the difference is increased, which can only be explained in terms of our theory by a higher loss factor for "d" than for A.

Now, measurements have shown approximately the same value for the two loss factors at very low frequencies, so it may be the frequency variation of η that is different for the two cases.

Let us now consider III.3.2, where the same comparison is made for the chip board floating floor. Here the difference in modulus is much less, approximately $0.26/0.13$ according to figure 5.4.2. This should mean at low frequencies a difference of about -2 dB, which means that type A should here give higher improvement.

This is indeed seen to be the case in III.3.2, although the difference is closer to -1 dB, but the modulus for "d" is rather approximate and may be closer to 0.12 MPa, which then agrees better with the measurement.

At higher frequencies we find the phenomenon we were looking for in III.3.1, that the forced solution continues higher up in frequency for d than for A. We may note that the first higher order resonance theoretically occurs at about 400 Hz for A and 800 Hz for "d", and the forced solution for "d" in III.3.2 also seems to continue about one octave higher than the forced solution for A.

At still higher frequencies the different force spectra for the chip board and the concrete come in, as previously discussed. However, the force spectra for the two chip boards should be almost equal, and we find approximately the expected difference between the two curves, $20 \log (6400/3600) = 5 \text{ dB}$. No explanation has been found for the sudden deviation of curve "d" at 1250 Hz. Up to that point the curves seem to start to diverge in a similar way as the curves in III.1.1.

6.1.4 Number of chip board layers

It was found rather puzzling after the series of measurements where the number of chip board layers was varied that almost no insulation improvement at all was achieved for extra layers of chip board. The result is shown in III.4.1

Again we start our search for explanation by consulting figure 5.4.2, and read the modulus for A at the three different load conditions. This gives $D_s = 0.26, 0.33, 0.42$ MPa respectively. We have previously pointed out, that these values are in good agreement with VI.1.1, where the static load on a single chip board is varied in steps, corresponding to the mass of one extra and three extra layers of chip board respectively. However, this change in the dynamic modulus is not at all sufficient to explain the curves in III.4.1. At low frequencies we expect a difference between (1) and (2) of about $40 \log (\sqrt{2} / \sqrt{0.33/0.26}) = 4$ dB, and between (1) and (3) of about 8 dB. At higher frequencies these differences are expected to be $20 \log (2 / \sqrt{0.33/0.26}) = 5$ dB and 10 dB respectively.

In III.4.1 the experienced results show approximately no difference between (1) and (2). About 2 dB difference is found between (1) and (3) at low frequencies, and no improvement (or negative!) at intermediate frequencies, and finally 2 - 3 dB at the highest measured frequencies.

We must search for an explanation in the force spectra of these different configurations. Force spectra were discussed in part 5.2, and measurement results shown in Appendix IV. None of our three configurations was measured directly there, but we have a measurement of a single chip board on 30 mm of mineral wool A. This should be almost identical to configuration (1). The spring stiffness may be a little lower here, giving $\eta_0 \approx 0.9$ instead of $\eta_0 = 1.15$, but the effect of this should be only marginal. However, when we introduce a second layer of chip board both m and B

are doubled, giving $\eta_0 \approx 0.5$ and also a doubled f_2 . When m and B are doubled again, we have $\eta_0 \approx 0.3$ and f_2 is doubled once more. Now we can probably assume that we have a situation similar to that of a chip board on concrete tiles shown in IV.2.4. Compared to the single chip board this gives the white part of the spectra 5 - 6 dB higher, and a break frequency, (here not the same as f_2) about an octave higher. This gives the largest differences, approximately 10 dB, at about 800 - 1200 Hz, but the difference decreases again at higher frequencies.

Here we have found an acceptable explanation. To the measured 2 - 3 dB improvement between (1) and (3) at low frequencies we add 5 - 6 dB for the difference in the white part of the force spectra. This agrees approximately with our expected 8 dB difference. At intermediate frequencies the force spectra differ by about 10 dB, which was the expected improvement here, and at the highest frequencies where the force spectra become more similar again, we measure a slight improvement in III.3.1.

The values for (2) are expected to lie somewhere in between. The white part of the force spectra may be increased 3 - 4 dB, here (a guess!) and at high frequencies we have a somewhat lower difference than the 10 dB estimated for the quadrupled construction.

We should point out that the result in III.3.1 only applies to the impact level improvement. Where the transmission loss improvement is concerned, the different force spectra have no influence, and our approximate "expected values" should apply directly. Because of the flanking transmission problem this could unfortunately not be verified by measurements.

6.1.5 Homogeneous concrete slab/concrete tiles as a floating floor

This measurement series is inspired by the work of Widén [23], who has made calculations and model measurements, where he compares a homogeneous floating floor and small tiles of the same material and thickness. Widén's calculations and model measurements show a relatively large improvement, when the tiles are used instead of the homogeneous slab (about 10 dB).

In our theoretical work we have not discussed this specific case, but we may do it now. The two floors have the same mass, and produce the same static load on the interlayer, which therefore should have the same properties for both types of floors. We have previously calculated the first flexural modes for the 350 x 350 mm concrete tile (see part 5.3). The first resonance frequency was calculated to be about 900 Hz, and we measured the first mode at about 1000 Hz. Below this frequency, we should only consider the tile floor as a lumped mass without any bending stiffness.

We consider first what happens at low frequencies. Here the interlayer can be considered as a spring, and we get Cremer's forced solution, except that the correction term $10 \log ((k_{B_3}/k_{B_1})^4 - 1)^2$ can be neglected, as the floating floor can be assumed to have a bending stiffness equal to zero. The improvement can thus be written simply as

$$\Delta L = 40 \log (f/f_1) \quad (1)$$

Compared to the homogeneous floating slab we have only an improvement of about 1 dB in the case of the concrete structural slab, but considerably more in the case of the lightweight slab. In figure 4.2.4

it is shown to be approximately 2.5 dB at low frequencies, and up to about 8 dB at high frequencies (2000 Hz).

This was the forced transmission. We have also the resonant transmission to consider, and this will be very much different for the two floating floors. At frequencies above approximately 300 - 400 Hz, we expect to notice the resonant transmission for the homogeneous slab. According to table 3.3.3.4 we have in the case of the lightweight structural slab

$$\Delta L_{\text{res}} = -10 \log \left(1 + \frac{0.13}{\eta_1} \left(\frac{k_{B_3}}{k_{B_1}} \right) \right) \quad (2)$$

or approximately -9 dB at f_{C_1} , and -17 dB at 2000 Hz if corrected bending waves are considered.

The corresponding term for the case of the concrete structural slab is

$$\Delta L_{\text{res}} = -10 \log \left(1 + \frac{0.03}{\eta_1} \right) \quad (3)$$

which gives about -4 dB at 500 Hz and -5 dB at 2000 Hz.

The resonant term of the concrete tiles cannot be evaluated with our theory in chapter 3, as we there assume equally sized floating and structural slabs with coupling along the edges. Here it is probably more appropriate to use Lindblad's approximate equation (3.2.19) as we pointed out at the end of part 3.2. This equation is

$$\Delta L = -10 \log \left(1 + \frac{\lambda_{B_1} U}{\pi^2 \eta_1 S} \right) \quad (4)$$

and it cannot be applied at frequencies below the first flexural mode of plate 1. In our case we may use it at frequencies above about 1000 Hz.

Equation (4) is independent of the structural slab, and for the η_1 values in Appendix V.1.9 we should have a ΔL_{res} of about -8 dB at 1000 Hz and about -5 dB at 5000 Hz.

The "wave medium solution" should be unaffected by the change from a homogeneous plate to tiles.

Now we can start comparing the measured curves. Let us first consider the 160 mm concrete structural slab and 50 mm of mineral wool A. In III.5.1 we show this configuration with the homogeneous floating slab as curve (1) and the concrete tiles as curve (2). We see that the tiles do indeed give higher improvement at low frequencies, but it is not a question of 1 - 2 dB, but rather 5 - 7 dB. We seem to have a different, lower spring stiffness in the interlayer. This is also seen in the intermediate frequency region. Even if we take into account 4 - 5 dB for the resonance transmission of the homogeneous slab, we still have a difference of 5 - 10 dB, which should not be there! This is difficult to explain. We have measured the force spectra for the tiles and the homogeneous slab respectively and found those to be almost identical.

In part 5.4 we referred to Kraak [46], where he discusses the influence of the air stiffness on the modulus of the interlayer. For a 350 x 350 mm test specimen of mineral wool A we found that below 25 Hz there was no influence of the air and above 2000 Hz we should have the combined stiffness (see table 5.4.1). This may influence our results here,

as there are openings between the tiles. If we disregard the air stiffness altogether at low frequencies, the modulus for A only decreases from about 0.55 to about 0.45, which only means 2 dB for the forced solution and 1 dB for the "wave layer solution".

We may only conclude that our theory seems inadequate when it comes to explain the large measured difference here.

At high frequencies we see that the curve for the tiles drops at about 1000 Hz as expected. Above this frequency the curve is very irregular, but the decrease from an extrapolated middle frequency line seems to be somewhat larger than the predicted -8 to -5 dB.

The irregularities above 1000 Hz are identical for all our measurements on the tile floors (see for example III.2.4), and seem to represent the very few flexural modes in the concrete tiles.

In III.5.2 we have very much the same phenomenon. Here we have the lightweight structural slab, but otherwise the same conditions as in III.5.1.

The difference between the two curves is larger here than in III.5.1. However, after we have corrected for the much higher resonant transmission and take into account the higher correction term in the forced solution, we end up with almost the identical unexplained difference.

We have also measured this last case for mineral wool B, and here we also have the configuration with a chip board on top of the tiles. (A solution which must be used in practice.) In III.5.3 these three cases are compared. To our surprise we see that adding

the chip board decreases the improvement for the tile floor. We have measured the force spectrum for this construction too (see Appendix IV.1.4) and found it to be identical to that of the reference slab up to a break frequency of about 800 Hz.

The mass of the floating floor is only marginally increased and adding the chip board should not influence our improvement curve below the break frequency. After correction for the different force spectra above 800 Hz we have a difference between curves (1) and (3) that agrees much better with our expected values. The difference is still a little too high at low frequencies, but at high frequencies the agreement is rather good. We also get a smoother curve at high frequencies as the hammers no longer excite the tiles directly.

The reason for the measured difference in III.5.3, when the chip board is added may be, that the board spreads the force more evenly. When a hammer strikes near an edge of a tile, the force is also distributed to the neighbour tile by the chip board. This excitement situation should suit our theoretical model better, and it should probably give a better agreement between airborne and impact sound insulation improvement.

6.1.6 Different structural slabs

In changing the structural slabs we only expect a change in the resonant transmission and in the correction term of the forced solution.

Let us consider the case shown in III.6.1 where we have the 50 mm concrete floating slab, interlayer A,

and our two different structural slabs. The difference at intermediate and high frequencies is in good agreement with our estimate in 6.1.5 of the resonant terms for the different constructions. At the highest frequencies this difference is a little too large, but this can be explained with the force spectra in Appendix IV. The lightweight slab has lower break frequency than the two other slabs.

At low frequencies the difference is surprisingly large. We would only expect the correction term for the forced solution to appear here, amounting to approximately 2 dB. The difference is however, 7 - 10 dB, and no reasonable explanation has been found.

In III.6.2 we compare tiles on interlayer A for the two structural slabs, and in III.6.3 we compare the chip board on interlayer A for the two structural slabs. The result in III.6.2 shows a small difference in the frequency region below 1000 Hz, where we expect the curves to be identical, as neither the forced correction term nor the resonant term should appear. The difference is, however, relatively small.

Above 1000 Hz we have assumed the resonant terms to be independent of the structural slab. However we measure a 5 - 6 dB lower value for the lightweight slab. This may suggest that the resonant term is coupled to the relation between the wavenumbers of the slabs even here.

The increase in the difference at the highest frequencies is again due to difference in force spectra.

Finally we expect the chip board curves to be identical for all frequencies as we assume both of the above correction terms to be negligible. The measurements show something else. An example is shown in III.6.3,

and similar relations can be found for other interlayers. The difference is very large for low frequencies, up to 10 dB. At high frequencies the curves become more similar, but the difference is still 2 - 5 dB. For other types of interlayer, the difference is not quite so large. As an example we show chip board on 50 mm of mineral wool C in III.6.4. The difference is less here, but still significant. No explanation has been found.

6.1.7 The influence of the thin resilient surface layer

In part 2.3.4 we stated that a thin resilient surface layer should not influence the impact insulation improvement if the measurements were made with the same layer on both the floating slab and the structural slab.

A part of our measurements were made in this way, and it may be interesting to investigate the influence of this parameter. In III.7.1 we show the improvement curves for the plastic carpet itself on three different constructions, the lightweight concrete, the chip board and the concrete slab. The curves for the concrete and the lightweight concrete are almost identical up to the break frequency for the lightweight concrete. The curve form is also as expected with a small improvement at low frequencies and steeply increasing improvement above a break frequency for the carpet, which seems to be approximately 600 Hz here.

We have already stated that the chip board itself has a break frequency at about 800 Hz because of the surface softness and a different kind of break frequency (because $Z_M < \omega M_0$) at about 500 Hz. Below

500 Hz we already have so high losses in the hammer blow, that the extra losses introduced by a resilient surface layer should not have any influence. For the hard surfaces this part of the improvement curve is about 1 - 2 dB according to III.7.1 Thus we would expect the improvement curve for the chip board to be 1 - 2 dB below the curves for the hard surfaces up to about 800 Hz, where it would start to drop. In III.7.1 we have, indeed, no improvement at the lowest frequencies, but only up to about 125 Hz. Here the curve rises above the hard-surface curves and continues as the highest curve up to the expected break frequency at about 800 Hz, where it starts to drop as predicted.

In III.7.2 the corresponding improvement for the carpet on the concrete tiles is shown, and it is seen to follow the curve for the concrete slab very well.

Our conclusion is that the impact level improvement should be practically identical for the floating floors with concrete surfaces, when measured with or without the plastic carpet. (The reference structural slab must of course have the same surface condition as the floating slab.) Only above 2000 Hz would we expect differences in the case of the lightweight structural slab.

However we cannot assume this to be true for the chip board. According to III.7.1 we would measure a higher improvement of 5 - 7 dB for intermediate frequencies, when the measurements are made with the plastic carpet than without it.

This is shown with direct examples in III.7.3 (chip board) and III.7.4 (concrete tiles). The different

results for the chip board have not been explained, but this fact should be remembered when using the figures from Appendix I.

Finally we show a measurement of the improvement for the plastic carpet by using the method of measuring the force level, as described in part 5.2. This was done for a concrete tile with and without the plastic carpet, and the result is shown in III.7.5. We can state that the improvement curve is identical to the ones measured by difference in impact level for the concrete surfaces. This supports the validity for our force level measurements, which are not a standard procedure.

6.1.8 Impact level/airborne sound insulation improvement

Our final comparison does not concern one special parameter. We try here to compare the measured impact level improvements to our most reliable measurements of the airborne sound insulation improvement. As previously discussed, this comparison is difficult because of flanking transmission, which affects the transmission loss measurements.

In Appendix II.1.7 we showed the measured transmission loss curves for an increasing width of interlayer in the case of a chip board floating floor and the lightweight structural slab. The thin 30 mm interlayer may be assumed to have a valid improvement curve in frequency regions where the difference to the highest curve is "sufficiently" large. In III.8.1 we have drawn the transmission loss improvement curve together with the impact improvement for this construction, and also the impact curve corrected for

the different force spectra according to Appendix IV.1.3 and IV.1.1. After this correction we would expect to have identical improvement curves. This is, however not the case as we can see in III.8.1.

First we may remark that ΔF is found for the chip board without the plastic carpet, and as we have shown in part 6.1.7, this correction is not identical for the case with the carpet. The corrected curve should probably be increased by about 4 - 5 dB above 125 Hz in accordance with III.7.3.

After this additional correction we would have an acceptable agreement between the two curves in III.8.1 at low frequencies. However, above 300 - 600 Hz the airborne improvement is much less than the impact one, and this cannot be blamed on flanking transmission as we can see in Appendix II.1.7. The ΔR -curve should be valid between 500 - 1200 Hz, and here the difference between ΔR and the corrected ΔL curve is 10 - 15 dB!

In Appendix II we also showed three measurements, where we measured the difference in acceleration level for the floating floor and for the structural slab by itself. These three measurements are shown in III.8.2 - III.8.4 together with the measured ΔR and ΔL curves for the three configurations. In III.8.2 and III.8.3 the three curves should be comparable without correction for the force spectra. In both cases we see that ΔR differs from ΔL_a , and this we can blame on flanking transmission. However, we would expect the curves for ΔL_a and ΔL to be identical, according to our theoretical considerations. Above f_{c1} we expect the radiation efficiency to be equal to one for both the floating floor and the structural slab, and the curves should surely be

identical. Below f_{c1} we may measure some resonant vibrations in the floating floor, that have a low radiation efficiency. In this frequency region we may therefore underestimate the insulation improvement.

No explanation has been found for the disagreement at high frequencies. We have investigated the fastening of the accelerometer by bees wax, but this was shown to give identical results to the fastening with screw. (See Appendix II.3.2.) We can only conclude that these measurements also indicate lower airborne insulation improvement than impact improvement for the floating floors.

In III.8.4 we must correct ΔL for the different force spectra before we make our comparison. We can, however, point out the relatively good agreement between ΔR and ΔL_a up to about 1000 Hz, where the flanking transmission seems to come into the picture. At low frequencies we have ΔR higher than ΔL_a , which we may try to explain as before that we measure resonant vibrations that radiate poorly. In III.8.5 we have made the ΔF correction and compare ΔL_a and $\Delta L + \Delta F$. We do not have identical curves here either. The airborne insulation improvement is even here lower than the impact improvement.

Finally in III.8.6 we may compare the measured ΔL_a for the tiles and the homogeneous floating slab as we did in part 6.1.5 for the impact improvement. The difference between the two ΔL_a curves corresponds much better to our expected difference than the difference we found for ΔL in III.5.1. The shape of the improvement curves themselves is however not quite in accordance to our theoretical model, primarily because of the low values above 1200 Hz.

6.2 Comparison between theoretical models and measurements

We would now like to make some direct numerical comparisons between our theoretical models from chapters 2, 3 and 4 and the measured results.

We have previously (in the above mentioned chapters) made some comparisons between different theoretical models for fictitious examples. In part 6.1 a thorough comparison between different measurement results was made. After the comparison in part 6.1 we can state that the measured curveform and the influence of different parameters agrees well in most cases with our theory in part 4.2 (and possibly 4.3) together with the resonant corrections from chapter 3. The most serious deviation from this theoretical model was, of course, the unexpected difference between airborne sound insulation and impact sound insulation improvement. We also had some difficulties in explaining the insulation improvement of the concrete tiles, and the measured improvement difference for a chip board floating plate when the structural slab was changed.

We will not compare the theoretical and measured results for all our configurations. We start with our "basic example" the two concrete slabs with interlayer A, and show how the measurements agree with our theoretical model. We do this in several steps, gradually modifying the model until best or acceptable agreement is found. Then we take several other configurations and compare the measured results with this modified model.

In our first example, we show our theoretical curve from part 2.3, which we have shown to be identical to Cremer's solution. We also show the theoretical curve from 4.2, which we have shown to be identical to

Lindblad's theory of the interlayer as a wave medium, if the slabs are "sufficiently" dissimilar. No resonant transmission has yet been introduced. Material properties are those presented in chapter 5. The most uncertain parameter is the loss factor of the interlayer. Here we have assumed a frequency independent value, $\eta_2 = 0.2$.

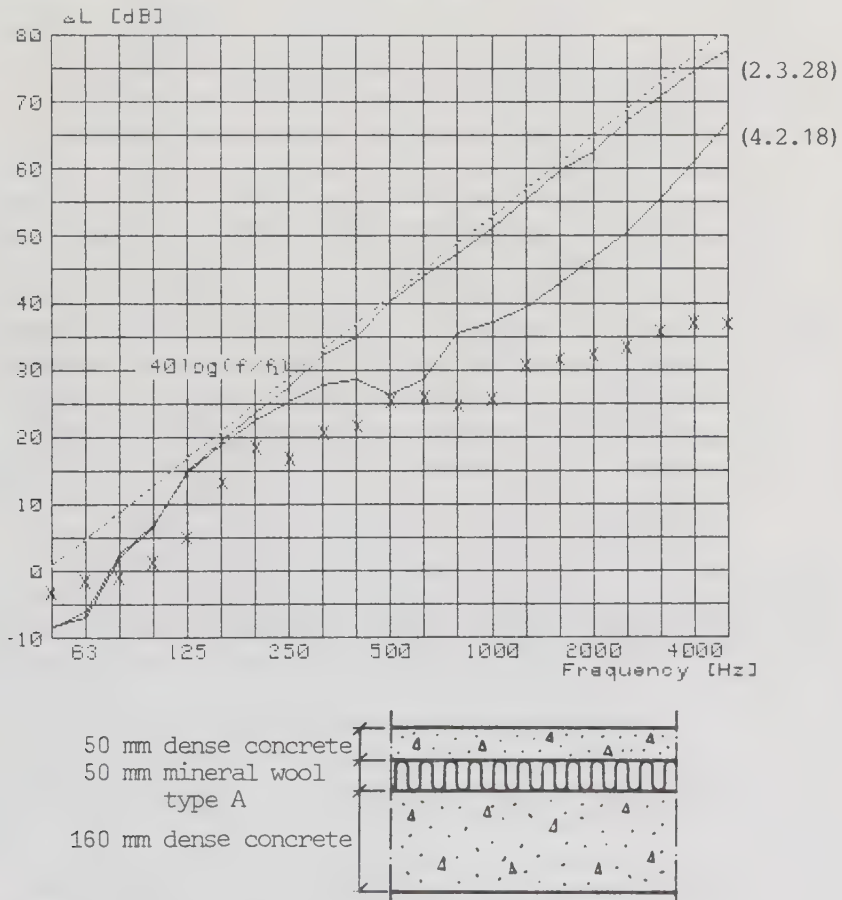


Figure 6.2.1. Comparison between the theoretical equations (2.3.28), (4.2.18) and measured impact insulation improvement (X).

We see that we overestimate the insulation improvement, especially at high frequencies. Now we introduce the correction because of the resonant transmission, which we discussed in chapter 3. The concrete structural slab is very close to being simply supported,

and we use the equation for Y_{SF} from the oblique incidence model of 3.3.3 (see table 3.3.3.4). The correction term is $\Delta L_{res} = -10 \log(1 + Y_{SF} \sigma_F)$, where σ_F has a very low value below f_{C1} and has approximately the value one above f_{C1} . To get a smooth transition around f_{C1} in our curves, we have assumed $\sigma_F = 0.1$ at $f_{C1}/2$, which increases to $\sigma_F = 1$ at f_{C1} . We assume $\sigma_F = 0$ below $f_{C1}/2$ and $\sigma_F = 1$ above f_{C1} . This correction gives the result shown in figure 6.2.2 below, where we show equation 4.2.18 before and after the resonant correction.

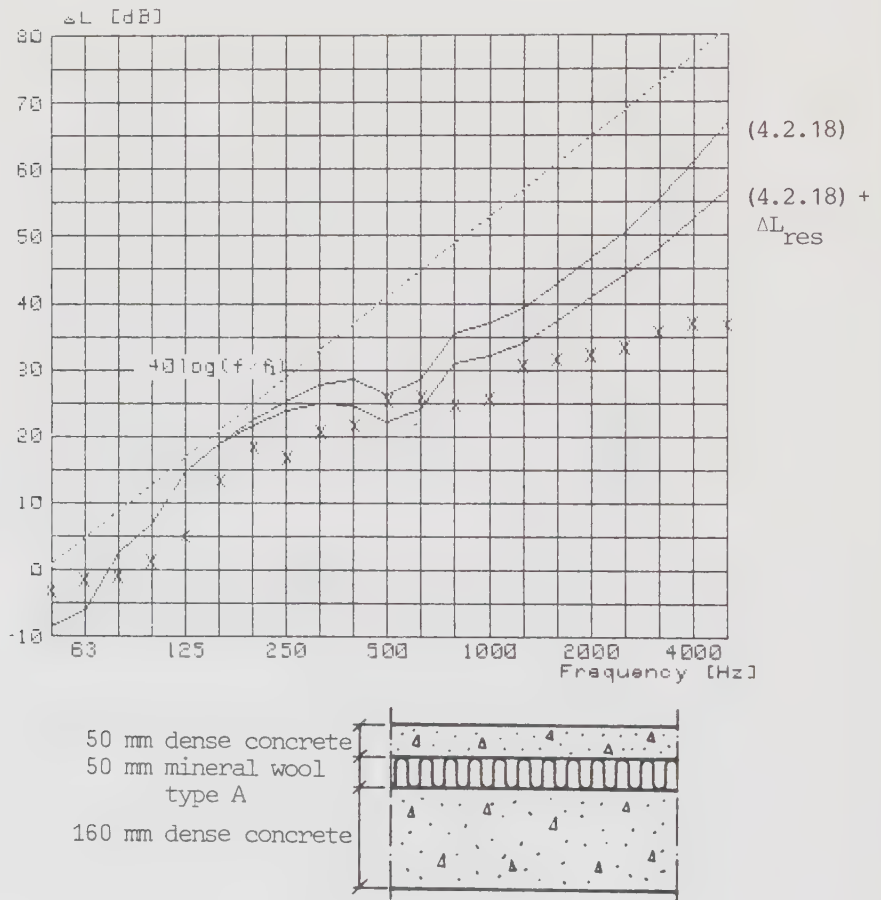


Figure 6.2.2. Comparison between the theoretical equation (4.2.18) with and without corrections for the resonant transmission. Measured results are also shown (X).

We see that we come a little closer to the measured results, but still we overestimate the improvement at the highest frequencies. The steep rise of the curves at high frequencies is due to the damping of the interlayer. We have assumed a relatively high loss factor ($\eta_2 = 0.2$), independent of frequency in lack of more exact knowledge.

The loss factor can be said to represent the damping per wavelength, and as the wavelength decreases, its influence over a fixed distance increases. It may not seem altogether unlogical to assume that the loss factor decreases in some way with increasing frequency. We may point out that the loss factor for the air wave in mineral wool in fact decreases with increasing frequency. No information, neither experimental nor theoretical is available on the frequency variation of a separate structural loss factor for the mineral wool.

Anyway we assume here in order to get a better agreement with the measured results, that η_2 decreases with increasing frequency. Our guess is that $\eta_2 \sim 1/\sqrt{f}$, and we have assumed $\eta_2 = 0.2 \sqrt{100/f}$ in the figure below.

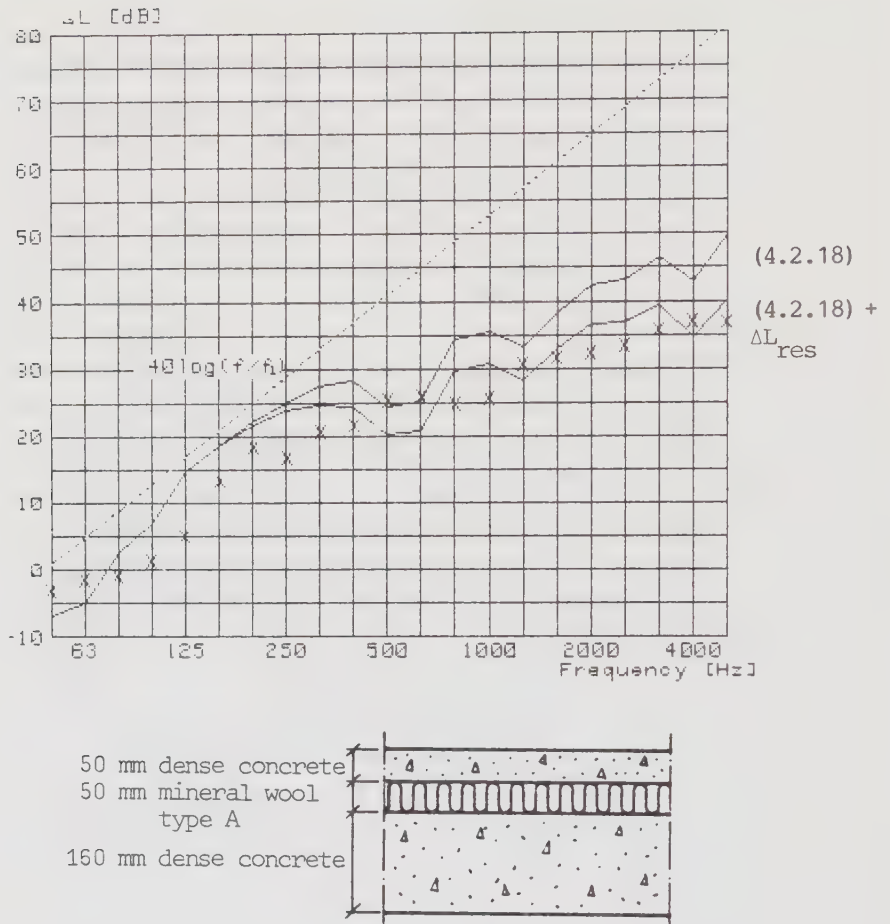


Figure 6.2.3. Comparison between the theoretical equation (4.2.18) with and without corrections for the resonant transmission. Note that we have here assumed $\eta_2 \sim 1/\sqrt{f}$. Measured results are also shown (X).

With this last correction we have achieved an acceptable agreement between our theoretical model and the measurements, at least for this particular configuration. We may however point out that we still overestimate the improvement somewhat at low frequencies. As

a final modification we try the theory from part 4.3 where we consider the anisotropy of the interlayer and the projected wavenumbers.

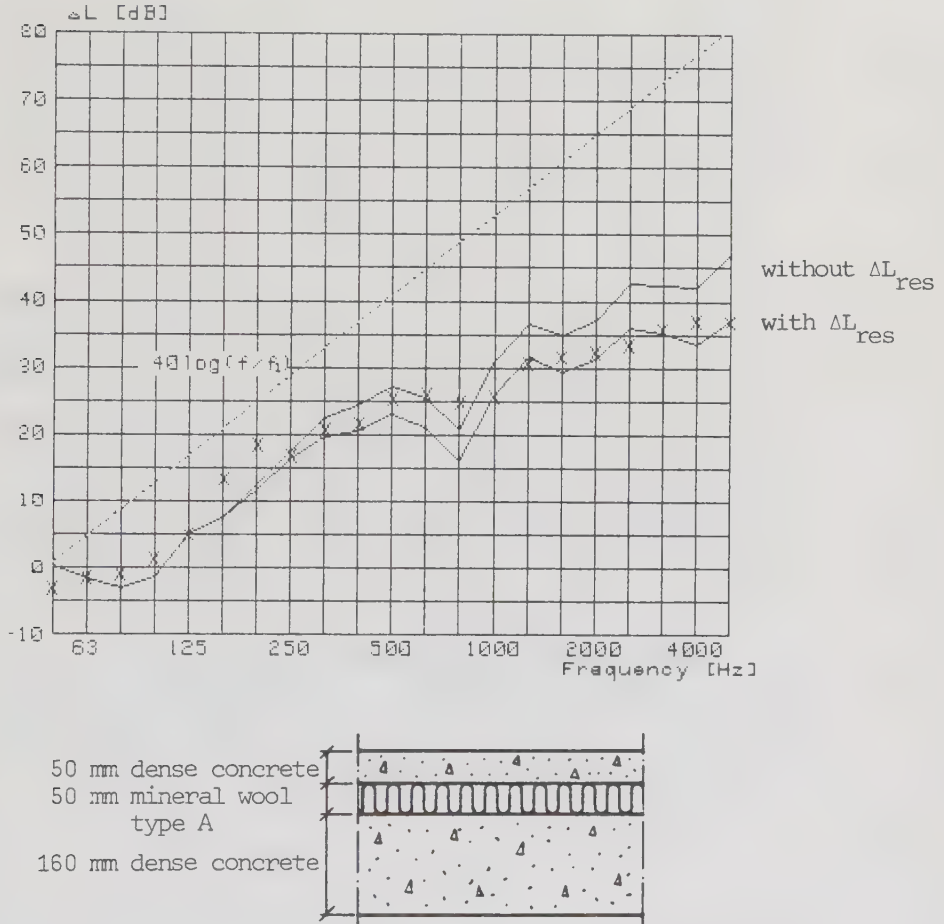


Figure 6.2.4. The theoretical equation (4.2.18) with projected wavenumbers in the anisotropic interlayer of equation (4.3.2). We also assume $\eta_2 \sim 1/\sqrt{f}$ and show the curves with and without the correction for resonant transmission. Measured results are also shown (X).

The last modification seems to give a better agreement with the measured results, although the valley at about 800 Hz is much deeper than in the measured results. We may point out there, that the model for the anisotropic interlayer in equation (4.3.2) is probably much too simple and the calculations based on that model are therefore only approximate. However, we seem to get a better agreement with our measured results, and the effect of the anisotropy when calculated with a more refined model, will probably lead to results with the same tendency.

We may point out that the measured curves are much smoother than the theoretical ones. Primarely it is the higher order resonances that cause the irregularity of the theoretical curves. By averaging over several points in each frequency band we may get a slightly smoother curve, but the resonance valleys will still be deeper here than for the measured results. The same is true for Lindblad's solution.

Our next example is the same configuration as the one above, except that we have a new interlayer. We have chosen the interlayer C in order to get large deviations from our previous example.

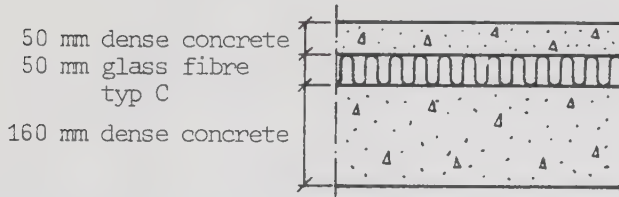
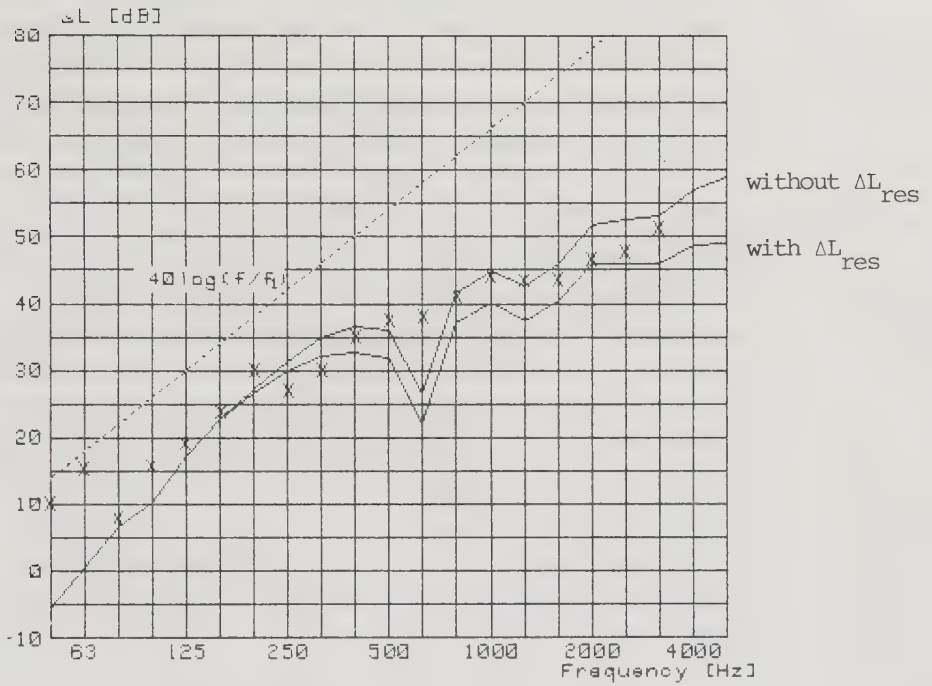


Figure 6.2.5. The theoretical equation (4.2.18) with the same assumptions as in figure 6.2.4. Measured results are also shown (X).

We have again a deep valley at the first "higher order resonance frequency" which does not appear in the measurements. Otherwise the agreement between measurements and theory can be said to be acceptable. We have large deviations at 50 and 63 Hz and at high

frequencies we have minor deviations, which can be explained by a slightly underestimated loss factor. We may point out that the calculations are only carried out at the center frequency for each $1/3$ -octave band, and averaging over the bands will probably give a smoother curve with more shallow valleys.

We would also like to investigate the influence of different widths of the interlayer. In figure 6.2.6 we show measured results for 30 mm and 120 mm of mineral wool "a". We intended to show the theoretical curves for both configurations, but as the curves were very much at the same level, the figure was difficult to interpret. Instead we show only the curve for the thin layer.

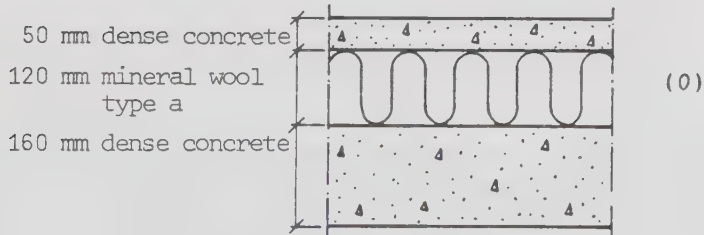
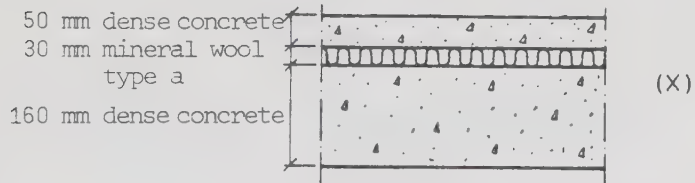
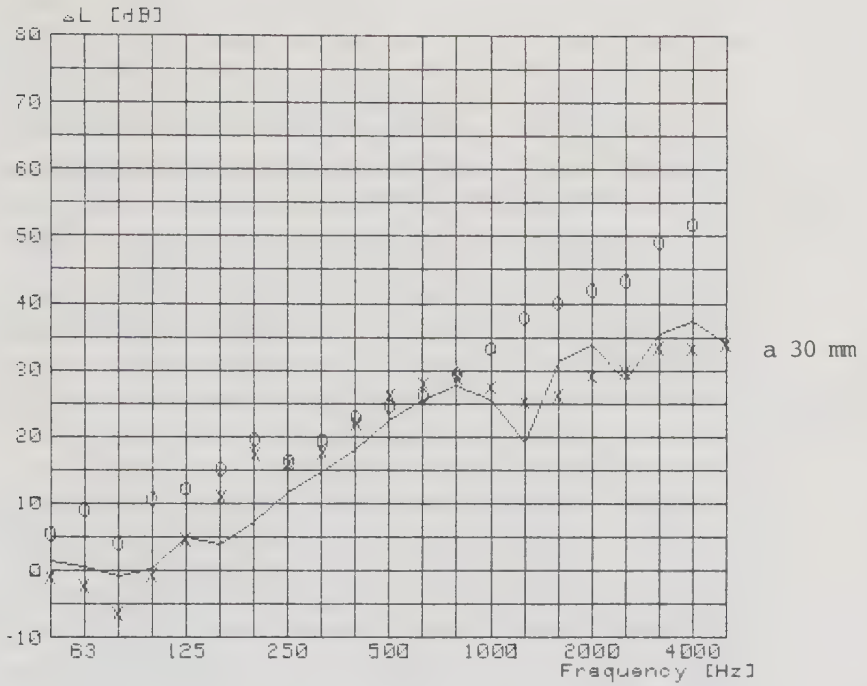


Figure 6.2.6. The theoretical equation (4.2.18) with the same assumptions as in figure 6.2.4. Measurement results for two different widths of the interlayer are shown together with the theoretical curve for the smaller width.

The agreement with the measurements is acceptably good for the 30 mm layer. The other curve had relatively good agreement up to about 800 Hz, but the expected rise because of a larger loss term did not show up. Instead the curve continued at about the same level as the curve for 30 mm of "a". We seem to have reduced the loss factor too strongly. Some sort of a compromise is needed, as frequency invariant η_2 gives too high damping and $\eta_2 \sim 1/\sqrt{f}$ gives too low damping. We may suggest $\eta_2 \sim (f)^{-1/4}$, but all this is just guessing, and a more exact knowledge of this point seems to be rather essential.

As a final example of configurations with the heavy structural slab we show a light floating floor, chip board on mineral wool A. In the figure below we show the curve from our adjusted theoretical model, and also this curve corrected with the difference in force spectra from Appendix IV.1.3. Corrections for resonant transmission are included, but they are less than 0.1 dB for all frequencies.

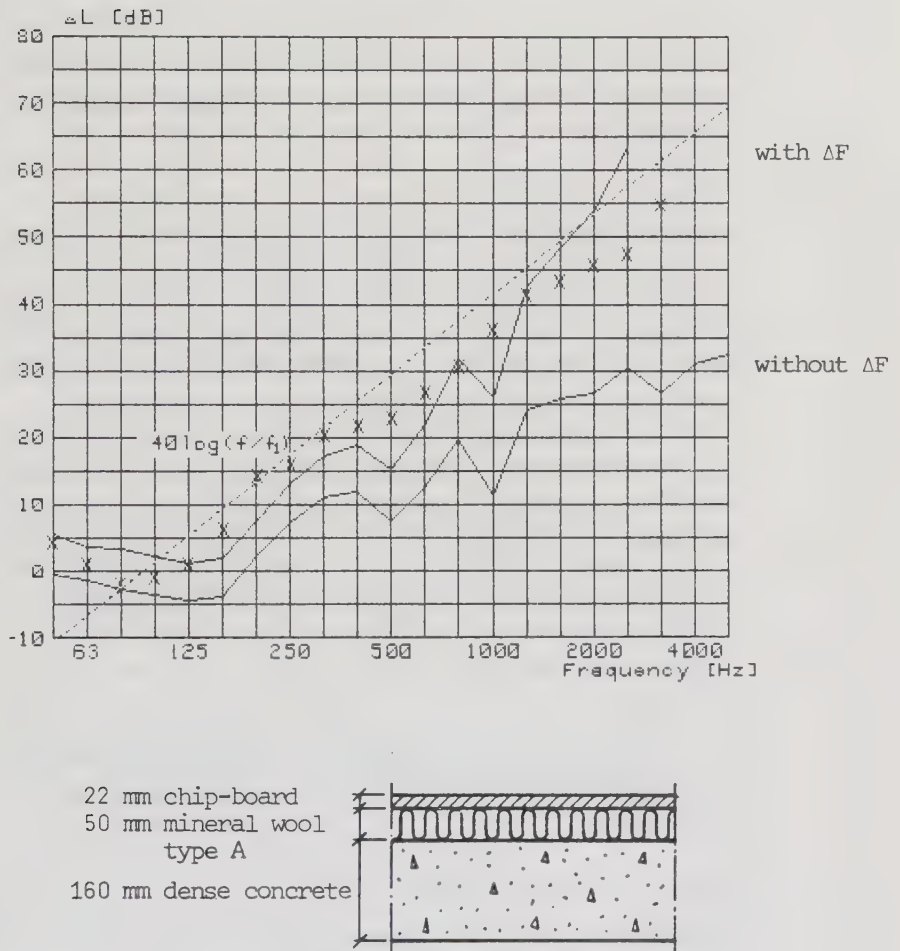


Figure 6.2.7. The theoretical equation (4.2.18) with the same assumptions as in figure 6.2.4. Curves with and without the measured correction for different force spectra are shown, and measured results are also presented (X).

We must confess that the agreement between measurement and theory is not perfect. However, if we smooth out the valleys, we may call the agreement acceptable, except at the highest frequencies, where it seems that

the force correction has become too large. This force correction was measured and the measurement results may have been a little uncertain at the highest frequencies, but the large difference in figure 6.2.7 is still rather surprising. The theoretical force correction:

$$\Delta F = 10 \log (1 + (f/550)^2) + 5 \text{ dB} \quad (1)$$

would probably give better agreement between theory and measurements. However, the break frequency because of the surface softness is not considered in (1) and according to our discussions in part 5.2 this will also have to be included.

As a final remark we may suggest, that the modulus for the interlayer at this low static load has been estimated too high in the extrapolated curve in figure 5.4.2. A lower value of the modulus would provide a better agreement in figure 6.2.7. Another possibility is that our model of the anisotropic medium does not apply as well here as in the case where the structural modulus is several times higher than the modulus of the entrapped air. (Here the two modules are approximately equal.)

In fact, calculations for the interlayer as a homogeneous medium show a better agreement with the measured results than the calculations for the anisotropic model. This is shown in the figure below.

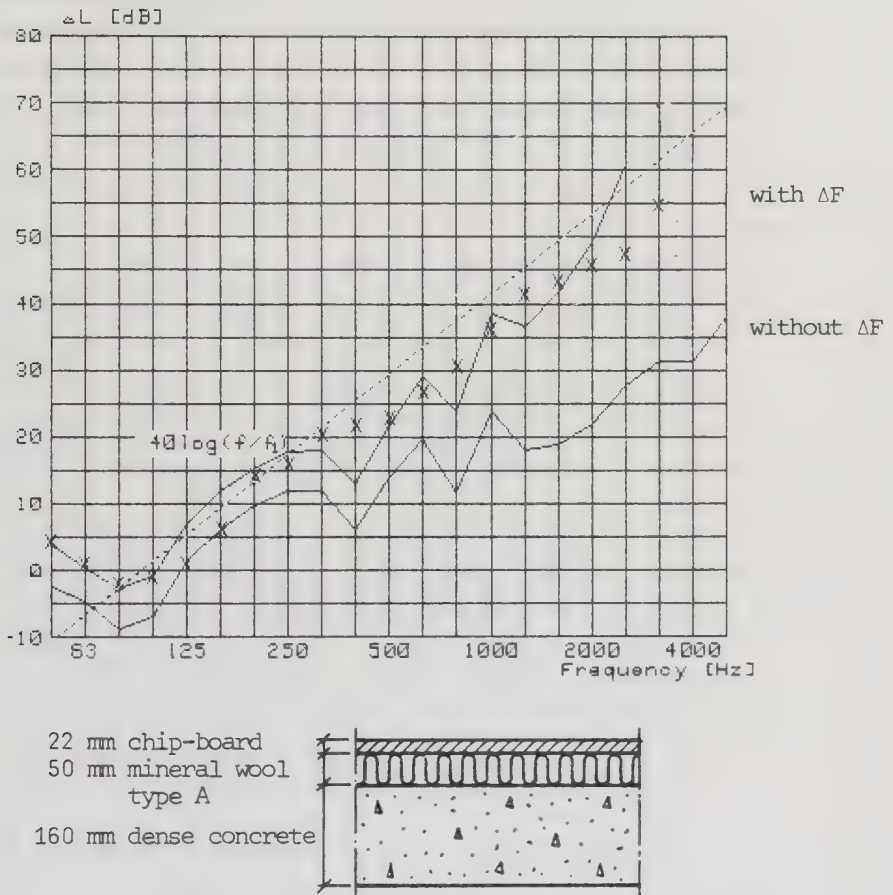


Figure 6.2.8. Same as figure 6.2.7, except that the interlayer is considered to be homogeneous here.

We have the same deviations at the highest frequencies, but otherwise we have better agreement than in figure 6.2.7.

We would also like to make some similar comparisons in the case of the lightweight structural slab. Let us start with the configuration 6.2.4 and change the structural slab. We make all the same assumptions as in

figure 6.2.4, and the result is shown in figure 6.2.9 below. Note that the calculations are made using the corrected bending stiffness B' from equation (4.2.20). We also assume the structural slab to be simply supported, and use the same theoretical model to estimate the resonant transmission.

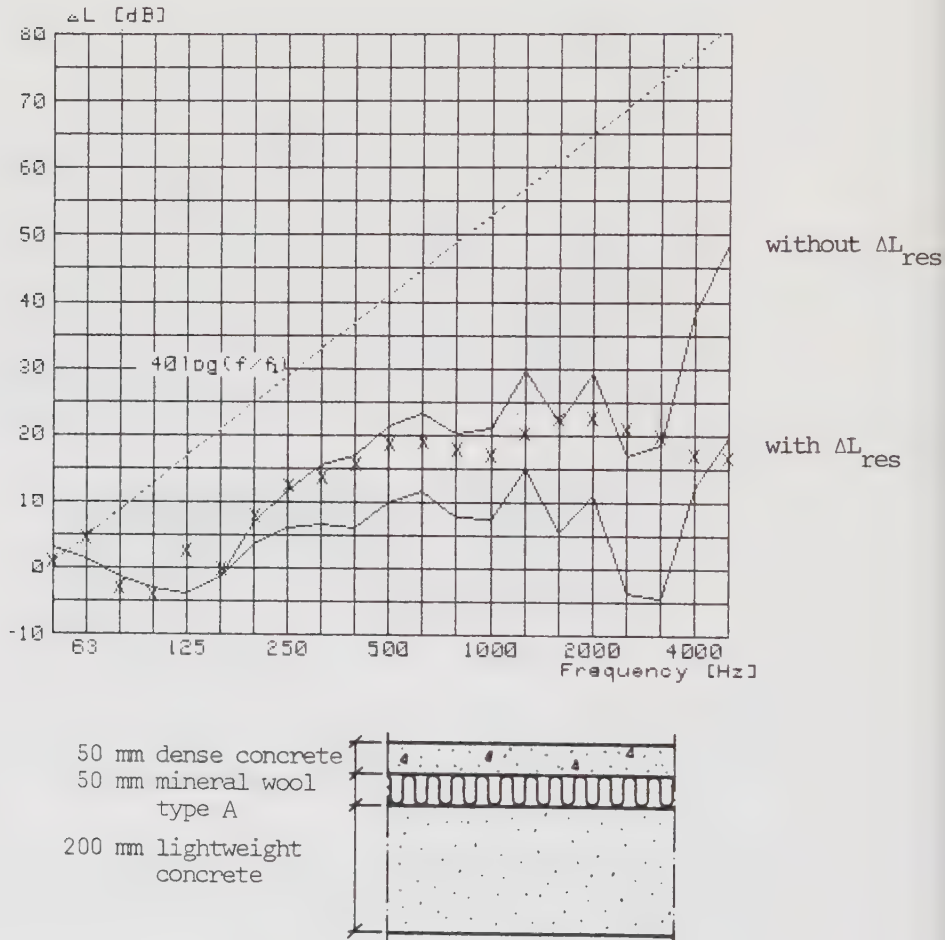


Figure 6.2.9. The theoretical equation (4.2.18) with the same assumptions as in figure 6.2.4. Measured results are also presented (X).

We see that the theoretical results are much too low compared to the measured ones. The resonant transmission seems to be very much overestimated.

We may recall that in part 5.3, when discussing the very different "edge absorption" of the two structural slabs, we pointed out the different boundary conditions of the two slabs. The concrete slab was surely very close to being simply supported, but the ends of the lightweight elements were in direct contact with the vertical concrete sides of the test opening. We may thus conclude, that rotation at the edges was prevented to some extent, and the model of a clamped structural slab might be more correct here.

In figure 6.2.10 we show the calculations with this new assumption, and find considerably better agreement between theory and measurements.

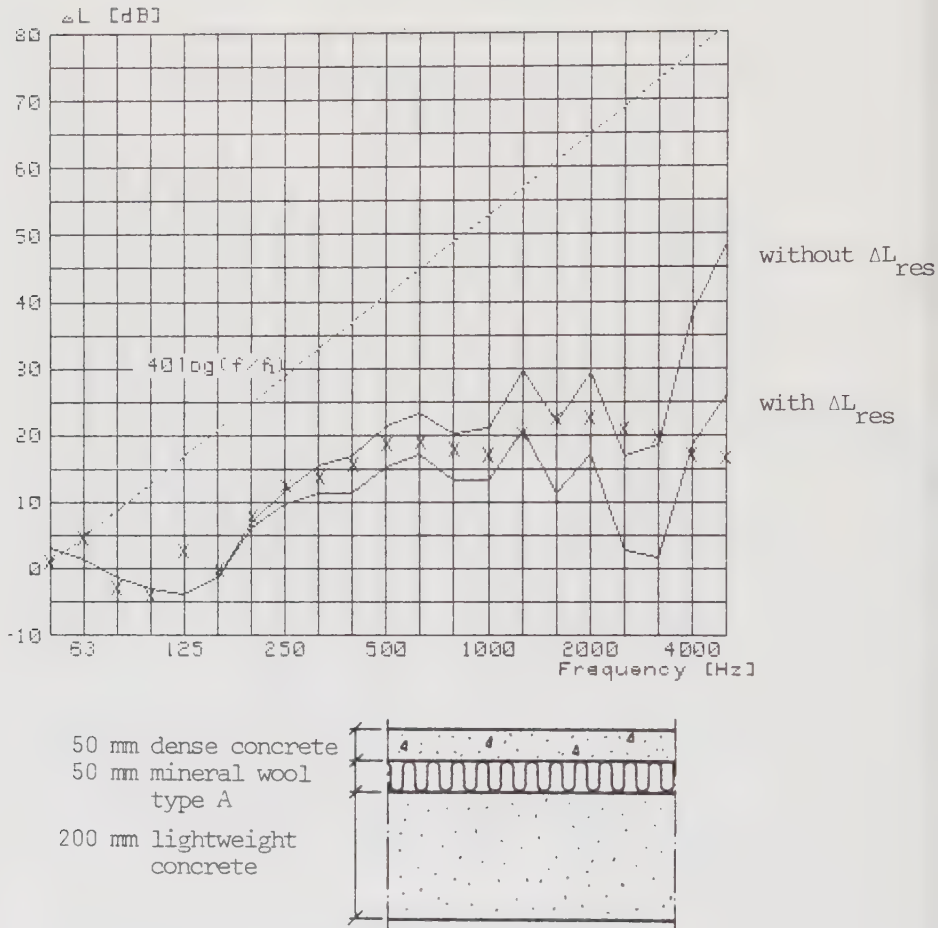


Figure 6.2.10. Same as figure 6.2.9, except that the structural slab is assumed to be clamped at the edges instead of simply supported.

The agreement between theory and measurements is not perfect here either, especially at the highest frequencies. We must, however, point out that the wave-numbers of the two plates become equal at about 3000 Hz. (We use the corrected bending stiffness B' from

equation 4.2.20.) This condition makes Cremer's forced solution unapplicable, and we have considerable irregularities in our Fourier transform solution.

No such irregularities are observed in the measured results, and we may ask if our corrected bending waves can be used so far above the limiting frequency for the simple bending wave theory.

Anyway, we may point out that after our assumption about a clamped structural slab we have an acceptable agreement up to about 1250 or 2000 Hz.

Finally we would like to show one attempt to calculate the insulation improvement for the floating floor of concrete tiles.

We show as an example the concrete tiles on inter-layer B and the lightweight slab. The calculations up to about 1000 Hz are made by setting the bending stiffness equal to zero and to introduce Lindblad's resonant corrections for $f > 1000$ Hz. (This procedure is discussed in part 6.1.5.)

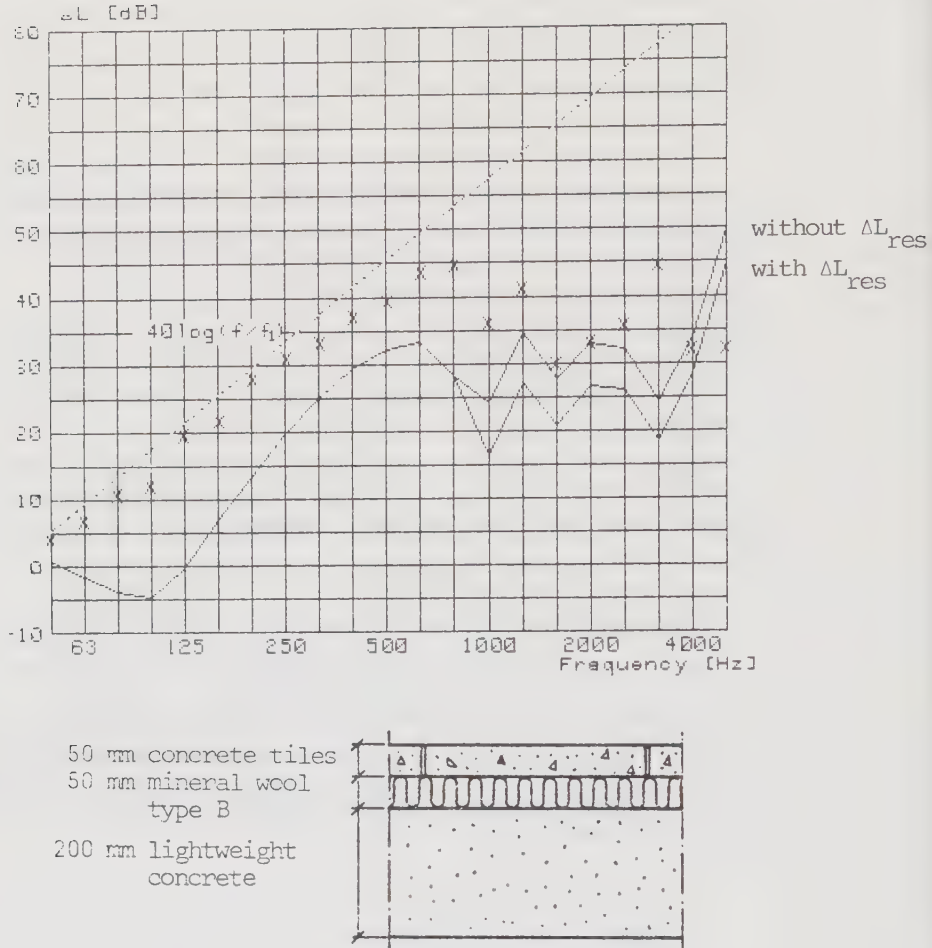


Figure 6.2.11. The theoretical equation (4.2.18) with the anisotropic interlayer of equation (4.3.2). We assume $\eta_2 \sim 1/\sqrt{f}$ and set the bending stiffness $B_1 = 0$ for $f < 1000$ Hz. For $f > 1000$ Hz we introduce corrections for resonant transmission according to equation (6.1.4). Measured results are also shown (X).

We do not have a very good agreement here between measurement and theory. We know however, that assuming the interlayer to be homogeneous will give us results closer to $40 \log(f/f_1)$ at lower frequencies. We do this in the figure below, where we also show the measured results for a chip board on top of the tiles.

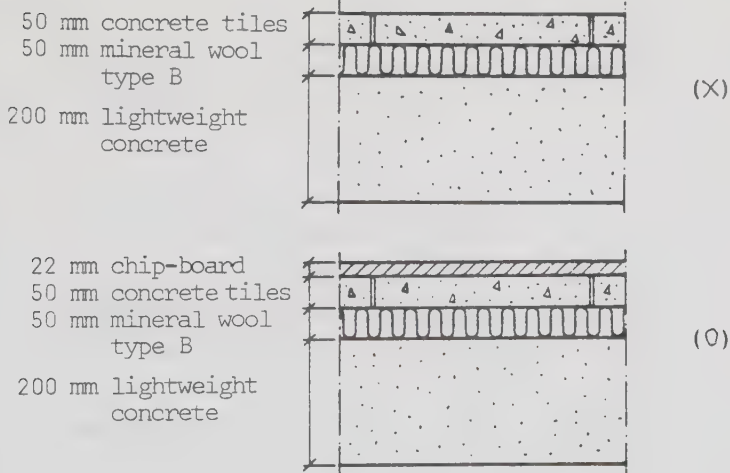
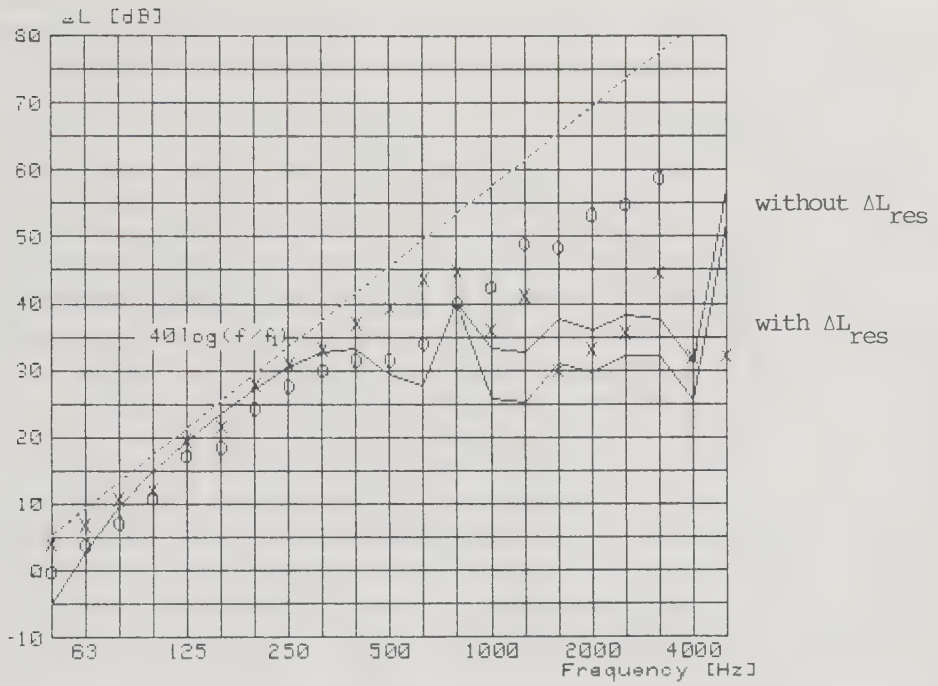


Figure 6.2.12. Same as 6.2.11, except that the inter-layer is assumed to be homogeneous here. Measured results are also shown (X), (O).

We see that we have much better agreement between measurements and theory here, especially if the results (o) are corrected for the different force spectra according to Appendix IV.1.4.

Just as for the example of a chip board floating floor, the model of a homogeneous interlayer gives much better results than the anisotropic model. The reverse is true for the homogeneous floating slabs. This different behaviour remains unexplained.

7. SUMMARY AND CONCLUSIONS

The problem of sound insulation of floating floors has been studied, both theoretically and through an extensive series of laboratory measurements.

The theoretical work is divided into three parts, and in each part different types of theoretical models are studied.

In the first part (chapter 2) we consider the model of locally reacting floating floors. Cremer's [1] classical work on this type of model, excited by a point force, is discussed briefly. Then we continue the discussions and consider the reciprocity of the sound insulation improvement, for the excitation of the structural slab and the floating slab respectively. We come to the conclusion that although the wave fields in the slabs are totally different for the two excitation cases, the insulation improvement will be identical. Our next step is to solve this problem of locally reacting floating floors by a different attack, which is the Fourier transform method. The problem is at first solved generally, and then as special applications we consider excitation by a point force and excitation by airborne sound. The point force case is shown to agree well with Cremer's classical solution, which we also call "the forced solution", but we are not confined to cases of $k_{B_1} \gg k_{B_3}$. The excitation by a diffuse field of airborne sound is shown to have a solution almost identical to the one for a point force. Finally in this first part we consider the relation between transmission loss and normalized impact level. Our most important conclusion there is that the sound insulation improvements for impact sound and

airborne sound are equal ($\Delta R = \Delta L$).

In the second part of the theoretical work (chapter 3) we consider the model of resonant transmission. We start with a historical review, where almost all the publications on floating floors we could find are discussed. We try to sort out the different theories and cast a light on how they are connected. The next step is to take a closer look at the few solutions, where the effect of the resonant transmission has been estimated quantitatively. For our example of a continuous interlayer we end up with only two available theories, one by Nilsson [10] and one by Lindblad [21], [22]. These two solutions can be written as correction terms to Cremer's classical solution. When written in this form, we can see that the two solutions differ considerably, and we come to the conclusion that each one has its different areas of application. Nilsson's solution applies when both the structural slab and the floating slab are of an equal size but have different boundary conditions. (If the boundary conditions are equal too, Cremer's solution can be used without corrections.) Lindblad's solution on the other hand is probably more correct in the cases of slabs of unequal size.

Nilsson's solution is deduced for a simply supported structural slab in a one-dimensional model. In our next step we consider such one-dimensional models with the help of a slightly different method, by using coupling integrals. We solve the cases of a simply supported slab and a clamped structural slab. Our solution for the simply supported case is equal to Nilsson's except for a numerical factor (2 instead of 3). Finally in this part we solve the two different boundary cases of the structural slab in a two-dimensional plate model, considering oblique incidence of

the bending waves. We get a semi-analytical expression for the simply supported case, but the clamped case has to be solved by numerical integration. In table 3.3.3.4 we present the different solutions, and numerical examples for three different configurations are shown. At the end of this part we also suggest a simple way to obtain an approximate solution for corrected bending waves, as one of our structural slabs is seen to have an extremely low limiting frequency for the applicability of simple bending wave theory.

The third and last part of our theoretical work is presented in chapter 4. Here we treat the interlayer as a wave medium. Again we start with a historical review, but only one available theory has been found. This is the work of Lindblad [32], [22] and his solutions are presented. Our next step is to extend our Fourier transform method to include the interlayer as a wave medium. A few (fictitious) examples are shown, and the influence of the variation of different parameters is discussed in words.

We also compare our solution to the one deduced by Lindblad. Our conclusion is that the two solutions will give almost identical results, if the two slabs are "sufficiently" dissimilar. In the case of more similar slabs, we have the same difference between the two solutions as the correction term of the forced solution. For cases, where corrected bending waves have to be considered, the two solutions differ more, as Lindblad's equations are not affected by a new bending stiffness in the slabs.

We continue the work by considering the effect of the anisotropy of the interlayer. This anisotropy is

considerable, as has been established by measurements. Our model for treating this anisotropy is however very simple, and the results can only be regarded as approximate. As a final theoretical piece of work we try to solve the problem of resonant transmission and wave medium behaviour of the interlayer in one model by using Statistical Energy Analysis (SEA). Our SEA solution is then compared to Lindblad's solution for dissimilar slabs, where it is almost identical to the extended Fourier transform method. The agreement is not very good, especially not for the case of a poorly damped floating slab. The SEA-method gives results far below the results of the two other methods.

Now over to the laboratory measurements. These are presented in Appendixes I - VII and discussed in chapters 5 and 6.

In the first part of chapter 5 we describe the acoustical measurements (see Appendix VII), the laboratory and the test objects, and 34 different impact level improvement measurements are presented in Appendix I.

The next part of chapter 5 considers the measurements of airborne sound insulation, which were made difficult because of flanking transmission. However we come to the conclusion that a few results should be valid and these are presented in Appendix II. These valid measurements were mainly made as measurements of the difference in acceleration level with and without the floating floor construction.

Chapter 5 continues with discussions on the measured force spectra for several slabs and floating floors. These measurements are shown in Appendix IV. The conclusion here is that we have only a marginal difference in force spectra between all concrete surfaces. However,

a considerable difference was found for the chip board floating floor compared to a concrete surface, and also for the lightweight concrete at high frequencies. To obtain theoretical results comparable to the impact level measurements we have to make corrections for this difference in force spectra. The same is true for a comparison between airborne sound insulation and impact sound insulation improvement.

The next part of chapter 5 considers the loss factors of the different floors and slabs. These have been measured for several constructions, and the results are presented in Appendix V. An attempt is made to divide the loss factor into inner losses and another part, which depends on the coupling losses to other construction elements. These coupling losses were found to be quite different for our two structural slabs, which may indicate different boundary conditions for the two. For the floating floors, a comparison was made to see if the coupling part of the loss factor could be explained by a radiation loading from the mineral wool. This was however seen to lead to overestimation of the loss factors as compared to the measurements.

In the last part of chapter 5 we consider the dynamic modulus and wave velocity of the interlayer. An important conclusion is the unlinear behaviour of the mineral wool and the effect of this phenomenon on the sound insulation properties of floating floors. An example is shown in Appendix VI.1. In figure 5.4.2 we have collected measurements of the dynamic modules of our interlayer materials at different static loads from different sources. We interpolate and extrapolate in this figure to get the values for our different load

conditions in the sound insulation measurements. To investigate further the variation of the dynamic modulus of the interlayer at different frequencies we made a series of measurements of the phase velocity of a propagating wave in a heap of mineral wool. (Examples of measurement results are shown in Appendix VI.2-VI.4.) These measurements seem to indicate that the dynamic modulus is approximately frequency invariant. They also seem to indicate that even at high frequencies we may add the stiffness of the enclosed air to the structural stiffness of the mineral wool. This structural stiffness can be measured by resonance methods at low frequencies for different static loads.

Finally in chapter 5 we point out that the important material property η_2 , the loss factor of the interlayer, is a very uncertain parameter. The only available values are measured at very low frequencies, and those values vary a lot between different references. Variations with different static loads are also reported. It is however the frequency variation of η_2 that we are most concerned about. The measurements of the sound insulation improvement compared to our theoretical models, imply that η_2 may decrease in some way with increasing frequency. No other evidence has been produced for this frequency dependence, and our final remark concerning this part of the report is to wish for a usable method to measure η_2 at different frequencies.

In the last part of the report (chapter 6) we discuss the results from the sound insulation improvement measurements. First we compare different measurements, where one parameter is varied at a time. These comparison curves are presented in Appendix III, and the

discussions are presented in part 6.1. We compare the influence of different widths of the interlayer, the influence of different materials in the interlayer and the influence of the number of chip board layers in the floating floor. We also compare a homogeneous concrete slab and concrete tiles of the same thickness as a floating floor. Different structural slabs are also considered, and finally we compare the sound insulation improvement for impact sound and airborne sound.

Secondly in part 6.2 we make direct numerical calculations on several configurations with material data from chapter 5. The results are compared to the corresponding measurement results. The theoretical model that we use is the extended Fourier transform method, where the interlayer is considered to be a wave medium. In the discussions in part 6.1 we found it however more convenient to divide the solution into different terms, similarly as in Lindblad's first presented solution [32], and consider one term at a time.

On the whole the theoretical model is seen to give acceptable agreement with the measurements. Still several questions remain unanswered. Besides the several cases of relatively small deviations, which may be assumed to be due to uncertain material properties, or in some cases uncertain measurement results or boundary conditions, we have two more important unexplained phenomena.

First it is the serious disagreement between airborne sound insulation and impact sound insulation improvement. This difference was significant for every one of the few cases which we could consider valid and

comparable. Most of the airborne results were not measurements of the transmission loss, but difference in acceleration level. The two should nevertheless have given identical results. Further investigations of this point in a laboratory, where flanking transmission does neither disturb the impact level measurements, nor the transmission loss measurements, would of course be interesting.

Secondly it is the fact that we have to model the interlayer differently for different types of floating floors to get an acceptable agreement with measurements. This is of course a serious drawback for our theoretical model. In all cases when the concrete slab was used as a floating floor, better agreement between measurements and theory is obtained, when the anisotropic wave medium model of the interlayer is used. However, in the case of concrete tiles or chip board as a floating floor, the model for a homogeneous wave medium gives better or much better results. No satisfactory explanation for this different behaviour has yet been found. A refined model for the anisotropic behaviour of the mineral wool might be able to explain this phenomenon.

As a final remark we may state that we have come close to a theoretical model, which we can use to predict the sound insulation improvement of different floating floor constructions with an acceptable accuracy. Some of the parameters used in the calculations may however be difficult to estimate accurately. We may mention the loss factor of the interlayer and the boundary conditions of the structural slab.

If a desk computer is available, the (more complete) Fourier transform method should probably be recommended, especially if the slabs are similar or corrected bending waves have to be considered. In quantitative discussions and approximate calculations it may however be more convenient to divide the solution into different terms, which can be discussed and calculated separately.

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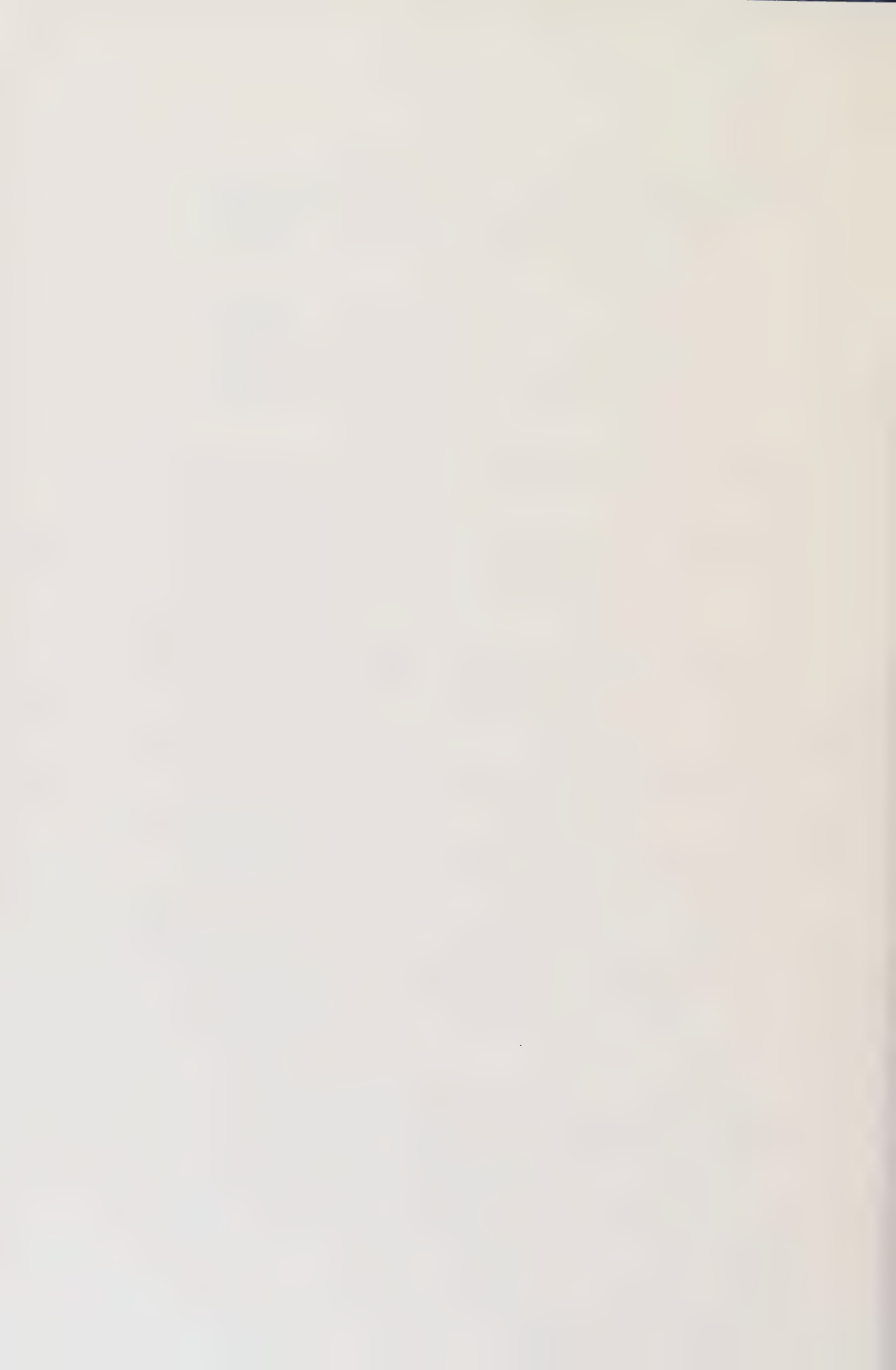
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APPENDIX I

APPENDIX I Impact level measurement

I.1 Structural slab: 200 mm lightweight concrete

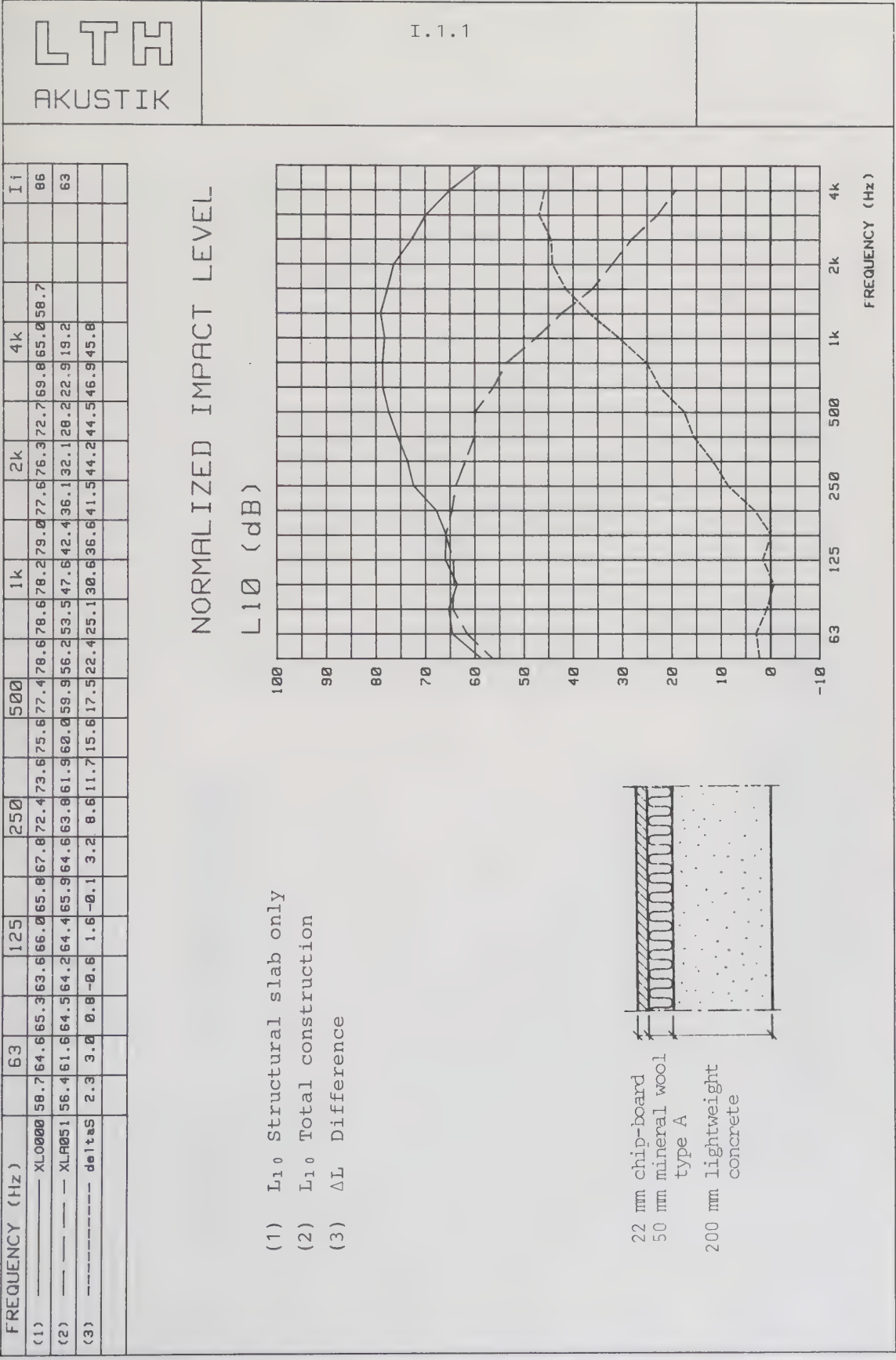
	FLOATING FLOOR	INTERLAYER
.1	chip board	50 mm A
.2	chip board	50 mm B
.3	chip board	50 mm C
.4	concrete slab	50 mm A
.5	concrete slab	50 mm B
.6	concrete tiles	50 mm A
.7	concrete tiles	50 mm B
.8	concrete tiles + chip board	50 mm B

I.2 Structural slab: 200 mm lightweight concrete 1.9 mm plastic carpet as surface layer in all measurements

.1	chip board	30 mm a
.2	chip board	60 mm a
.3	chip board	120 mm a
.4	single chip board	50 mm A
.5	double chip board	50 mm A
.6	quadruple chip board	50 mm A
.7	chip board	50 mm g
.8	chip board	50 mm d
.9	concrete slab	50 mm A
.10	concrete slab	50 mm B
.11	concrete tiles	50 mm A
.12	concrete tiles	50 mm B
.13	concrete tiles + chip board	50 mm A

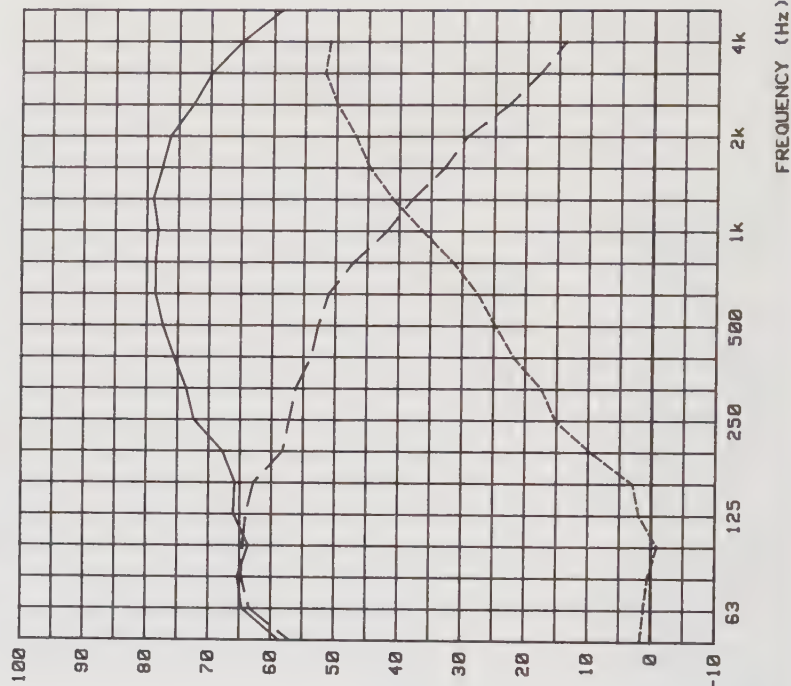
1.3 Structural slab: 160 mm concrete

.1	chip board	50 mm A
.2	chip board	50 mm B
.3	chip board	50 mm C
.4	chip board	30 mm a
.5	chip board	20 mm d
.6	concrete slab	50 mm A
.7	concrete slab	50 mm B
.8	concrete slab	50 mm C
.9	concrete slab	20 mm a
.10	concrete slab	60 mm a
.11	concrete slab	120 mm a
.12	concrete slab	20 mm d
.13	concrete tiles	50 mm A

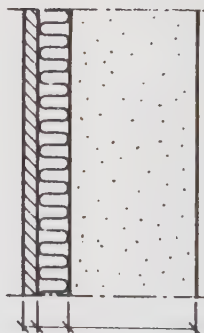


NORMALIZED IMPACT LEVEL

L_{10} (dB)



- (1) L_{10} Structural slab only
- (2) L_{10} Total construction
- (3) ΔL Difference



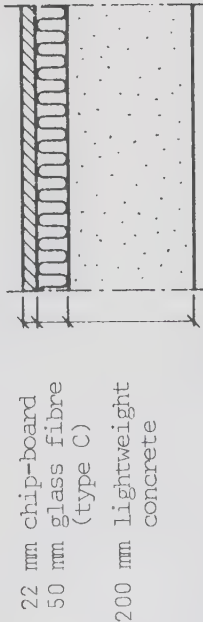
22 mm chip-board
 50 mm mineral wool
 type B
 200 mm lightweight
 concrete

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i												
(1) ——— XL0000	58.7	64.6	65.3	66.0	67.8	72.4	73.6	75.6	77.4	78.6	78.6	77.6	76.3	72.7	69.8	65.0	58.7	86		
(2) ——— XL0051	57.0	63.4	64.7	64.5	64.0	62.8	58.2	56.2	53.8	52.7	51.1	47.2	41.9	38.0	32.9	29.3	22.8	17.9	13.9	60
(3) ----- deltas	1.7	1.2	0.6	-0.9	2.0	3.0	9.6	15.2	17.4	21.8	24.7	27.5	31.4	36.3	41.0	44.7	49.9	51.9	51.1	

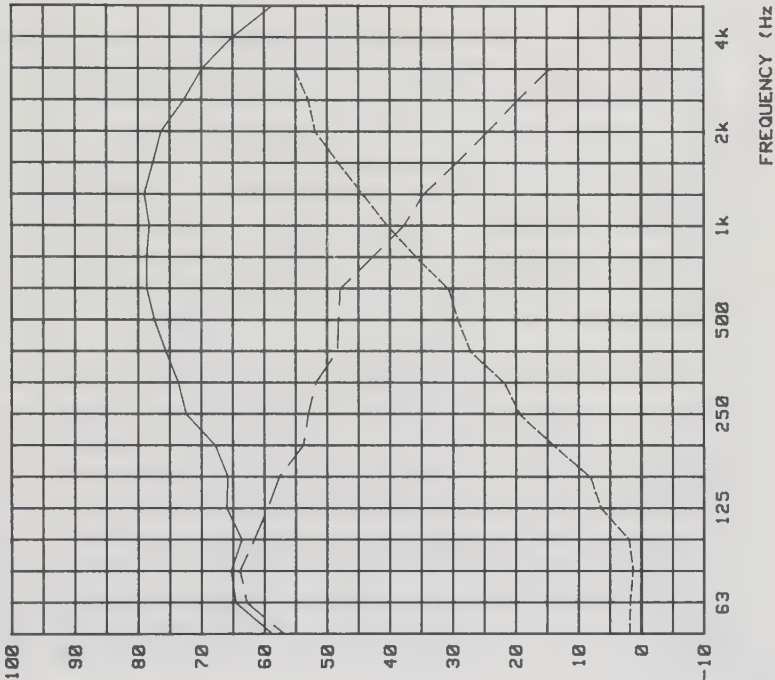
NORMALIZED IMPACT LEVEL

L10 (dB)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I1													
(1) ----- XL0000	58.7	64.6	65.3	63.6	66.0	65.8	67.8	72.4	73.6	75.6	77.4	78.6	78.2	79.0	77.6	76.3	72.7	69.8	65.0	58.7	86
(2) ----- XL0051	56.8	62.8	63.9	61.6	59.5	57.6	53.8	53.0	51.8	48.4	48.2	47.9	42.8	37.8	34.7	29.4	24.4	19.7	14.6		57
(3) ----- deltaS	1.9	1.8	1.4	2.0	6.5	8.2	14.0	19.4	21.8	27.2	29.2	30.7	35.8	40.4	44.3	48.2	51.9	53.0	55.2		

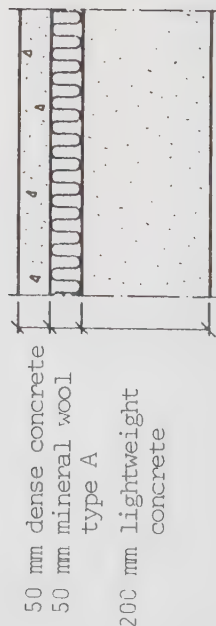
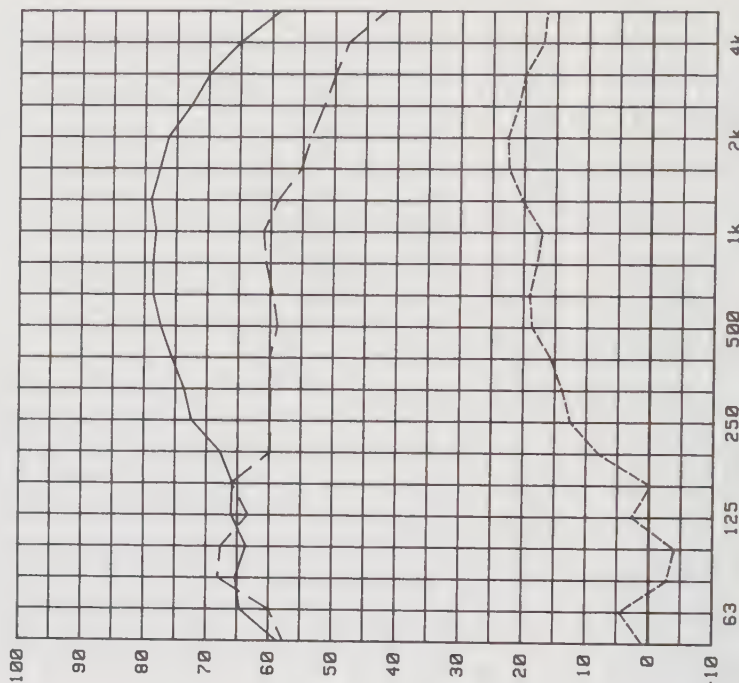


FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i															
(1) ——— XL0000	58.7	64.6	65.3	63.6	66.0	65.8	67.8	72.4	73.6	75.6	77.4	78.6	78.6	78.2	79.0	77.6	76.3	72.7	69.8	65.0	58.7	86	
(2) ——— XL005p	57.7	60.0	60.1	67.6	63.3	66.0	59.8	59.9	59.8	60.0	58.0	59.8	59.8	60.7	61.1	58.8	55.2	53.7	51.7	49.9	47.9	42.1	67
(3) ——— deltaS	1.0	4.6	-2.8	-4.0	2.7	-0.2	8.0	12.5	13.8	15.6	18.6	19.0	17.9	17.1	20.2	22.4	22.6	21.0	19.9	17.1	16.6		

NORMALIZED IMPACT LEVEL

L10 (dB)

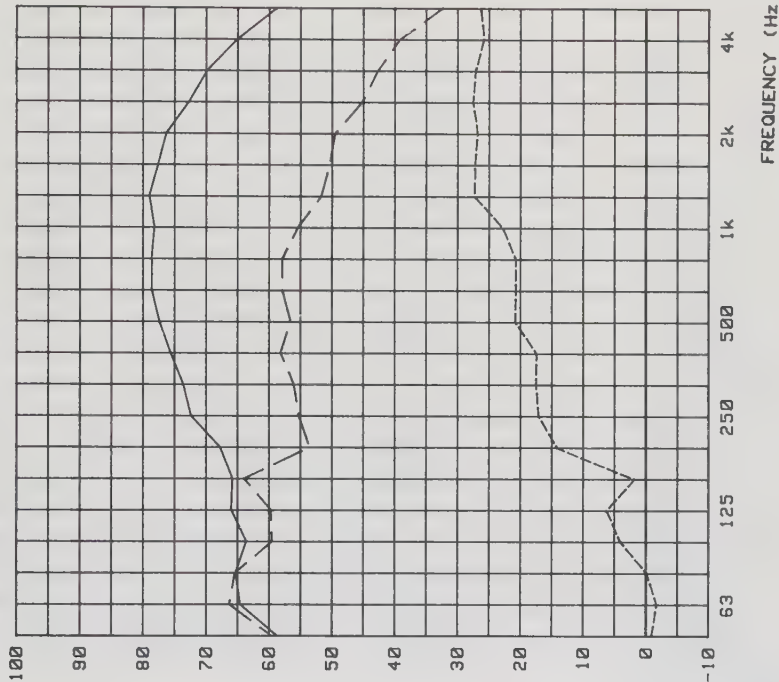
- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



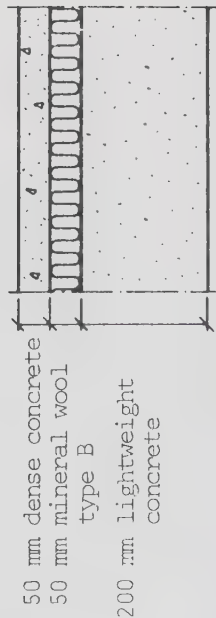
FREQUENCY (Hz)

NORMALIZED IMPACT LEVEL

L10 (dB)



- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

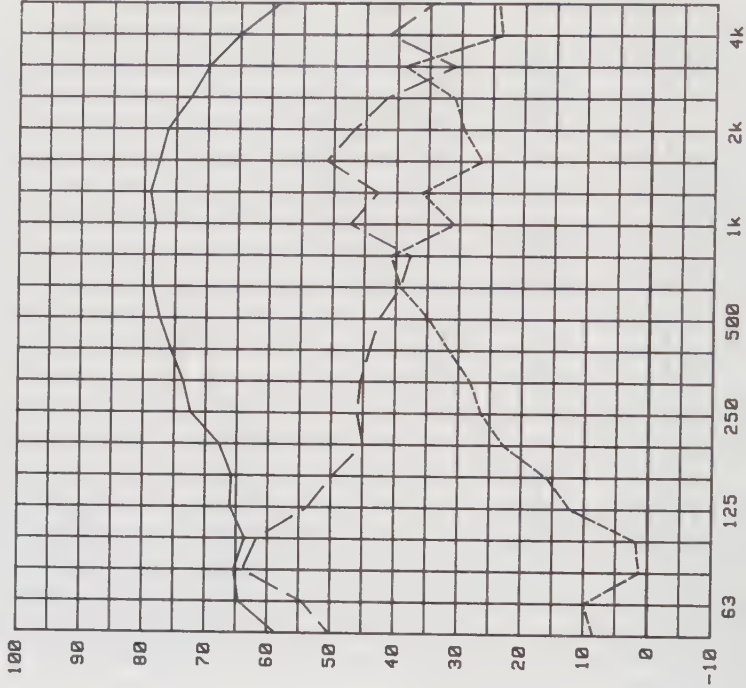


FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i													
(1) ——— XL0000	58.7	64.6	65.3	63.6	66.0	65.8	67.8	72.4	73.6	75.6	77.4	78.6	78.6	78.2	79.0	77.6	76.3	72.7	69.8	65.0	58.7
(2) ——— XL005t	50.2	54.5	63.9	61.7	53.7	49.8	45.0	46.0	45.5	44.1	42.6	39.3	37.8	47.3	43.0	51.1	46.8	41.7	30.9	41.4	34.5
(3) ——— del*AS	8.5	10.1	1.4	1.9	12.3	16.0	22.8	26.4	28.1	31.5	34.8	39.3	40.8	30.9	36.0	26.5	29.5	31.0	38.9	23.6	24.2

NORMALIZED IMPACT LEVEL

L10 (dB)

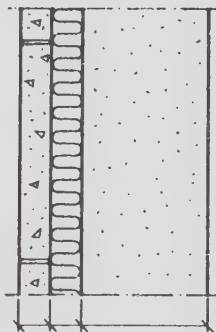
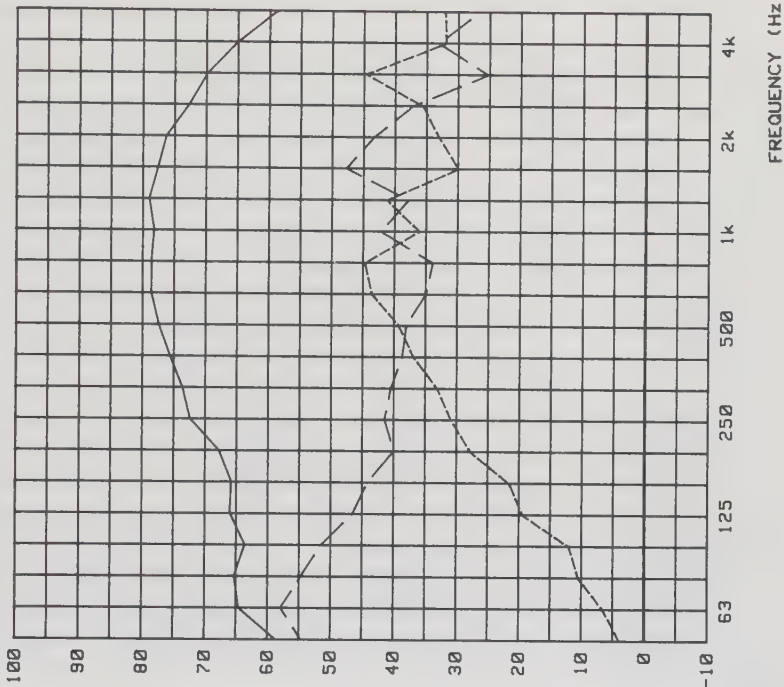
- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



FREQUENCY (Hz)

NORMALIZED IMPACT LEVEL

L10 (dB)



50 mm concrete tiles
 50 mm mineral wool
 type B
 200 mm lightweight
 concrete

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

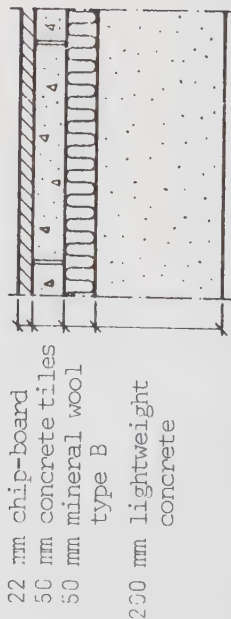
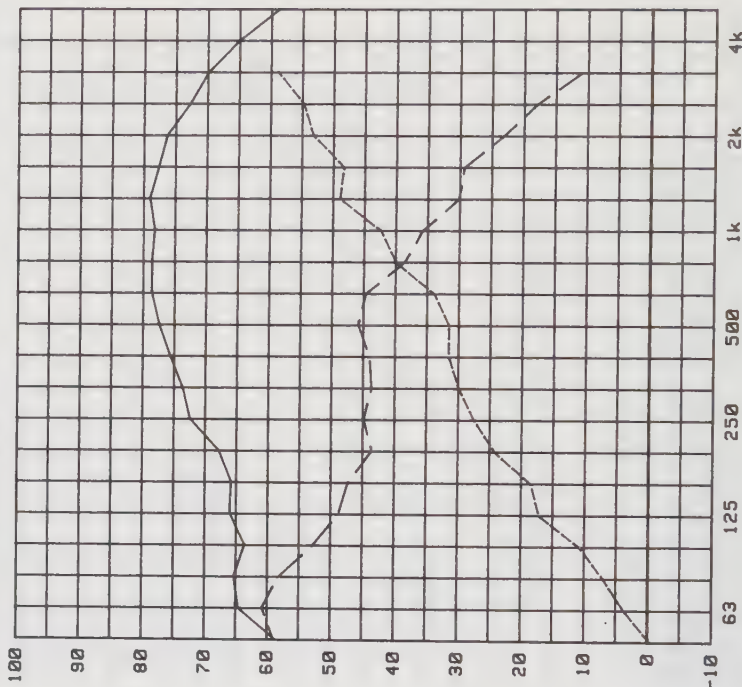
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i														
(1) ——— XL0000	58.7	64.6	65.3	63.6	66.0	65.8	67.8	72.4	73.6	75.6	77.6	76.3	72.7	69.8	65.0	58.7	86					
(2) ——— XL005t	54.7	57.9	54.7	51.5	46.5	44.2	40.0	41.5	40.5	38.7	38.1	35.0	33.9	42.2	37.8	47.7	43.3	37.1	25.2	33.1	26.6	54
(3) ----- delXBt	4.0	6.7	10.6	12.1	19.5	21.6	27.8	30.9	33.1	36.9	39.3	43.6	44.7	36.0	41.2	29.9	33.0	35.6	44.6	31.9	32.1	

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I i													
(1)	— XL0000	58.7	64.6	65.3	63.6	66.0	65.8	67.8	72.4	73.6	75.6	77.4	78.6	78.2	79.0	77.6	76.3	72.7	69.8	65.0	58.7	86
(2)	— — — — XL005u	59.0	60.8	58.2	52.8	48.8	47.3	43.6	44.9	43.7	44.1	45.9	44.7	38.6	35.8	30.1	29.3	23.1	17.9	10.9		48
(3)	— — — — — delXBu	-0.3	3.8	7.1	10.8	17.2	18.5	24.2	27.5	29.9	31.5	33.9	40.0	42.4	48.9	48.3	53.2	54.8	58.9			

NORMALIZED IMPACT LEVEL

L10 (dB)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

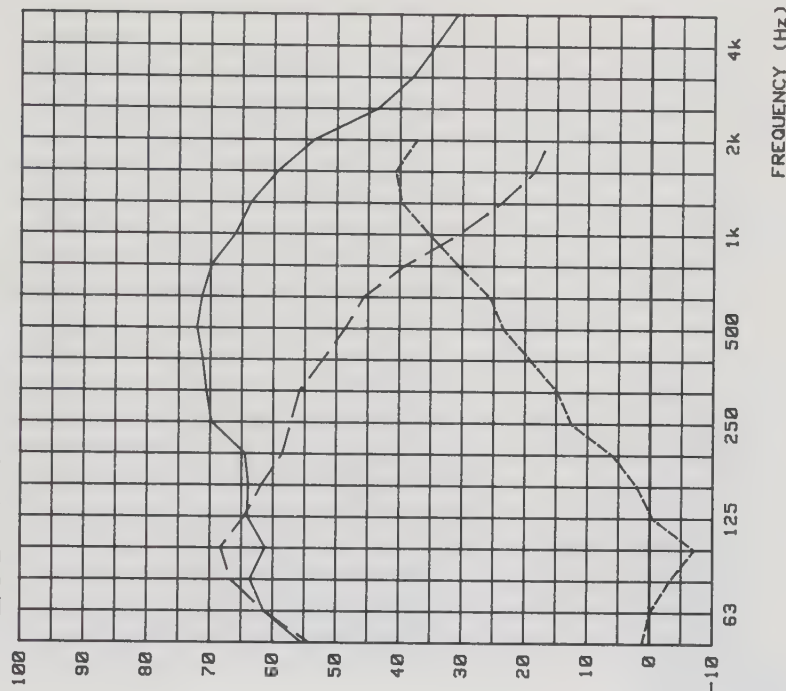


FREQUENCY (Hz)

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I1													
(1) ——— SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	70.6	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9
(2) ——— SL0031	54.2	61.4	66.7	68.3	64.5	61.8	58.5	57.2	55.8	52.1	48.6	45.7	39.2	30.7	23.9	18.6	16.3				
(3) ——— deltaS	1.2	0.0	-3.1	-7.1	-0.4	2.1	6.0	12.6	14.8	19.1	23.5	25.6	30.6	35.3	39.7	40.7	37.3				

NORMALIZED IMPACT LEVEL

L10 (dB)

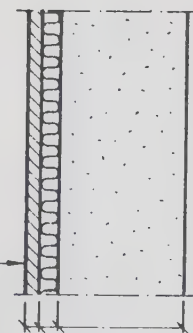


1.9 mm plastic carpet



200 mm lightweight concrete

1.9 mm plastic carpet



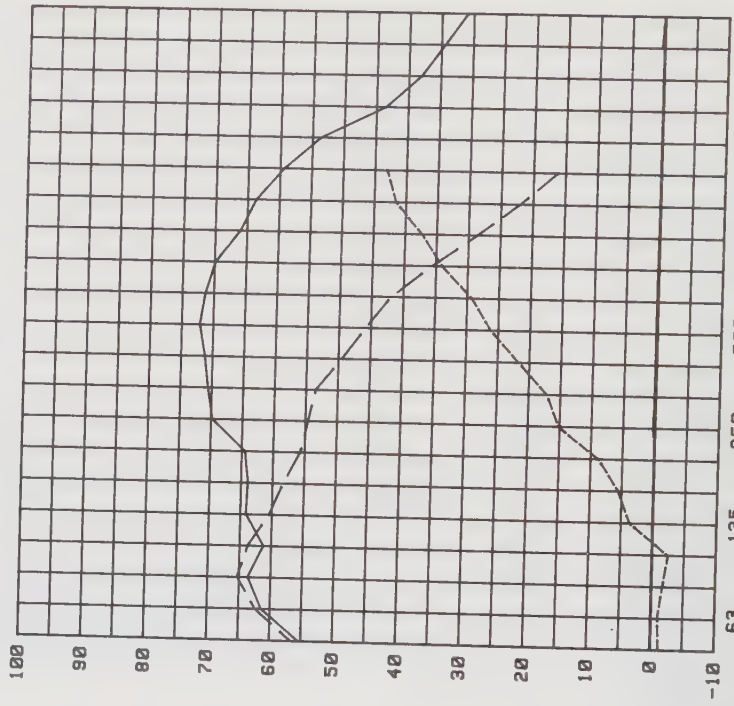
22 mm chip-board
30 mm mineral wool type a
200 mm lightweight concrete

Difference (3)

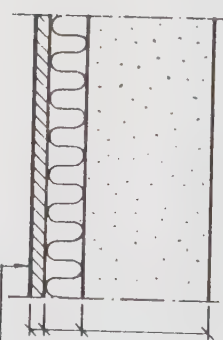
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I i												
(1)	——— SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	68.8	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9
(2)	——— SL0061	56.6	62.4	65.3	63.7	60.4	58.3	55.7	54.6	53.5	49.5	45.9	42.0	35.7	28.6	22.0	16.2				
(3)	----- delta06	-1.2	-1.7	-2.5	3.7	5.6	8.8	15.2	17.1	21.7	26.2	29.3	34.1	37.4	41.6	43.1					

NORMALIZED IMPACT LEVEL

L10 (dB)



(1)
1.9 mm plastic carpet
200 mm lightweight concrete



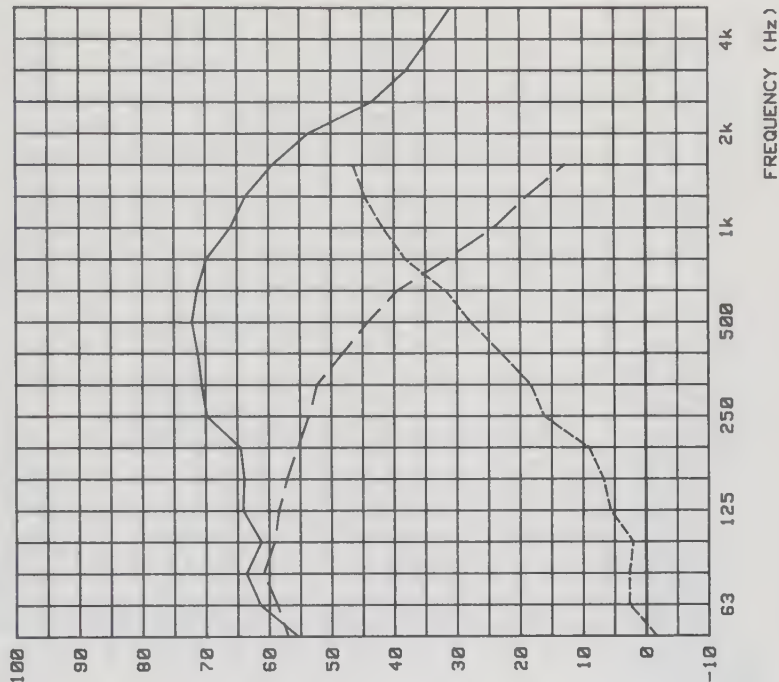
(2)
1.9 mm plastic carpet
22 mm chip-board
60 mm mineral wool type a
200 mm lightweight concrete

(3)
Difference

FREQUENCY (Hz)

NORMALIZED IMPACT LEVEL

L10 (dB)



(1)

(2)

(3)

1.9 mm plastic carpet

200 mm lightweight concrete

1.9 mm plastic carpet

22 mm chip-board
120 mm mineral wool type a
200 mm lightweight concrete

Difference

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	Ii													
(1)	— SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9	72
(2)	— SL0051	56.2	61.1	63.6	63.7	61.8	60.2	58.2	55.9	55.5	52.4	48.5	44.3	38.8	31.3	25.3	19.9	17.1				59
(3)	— deltaS	-0.0	0.3	0.0	-2.5	2.3	3.7	6.3	13.9	15.1	18.8	23.6	27.0	31.0	34.7	38.3	39.4	36.5				

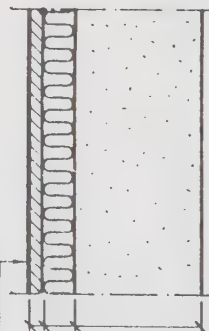
NORMALIZED IMPACT LEVEL

1.3 mm plastic carpet
200 mm lightweight concrete



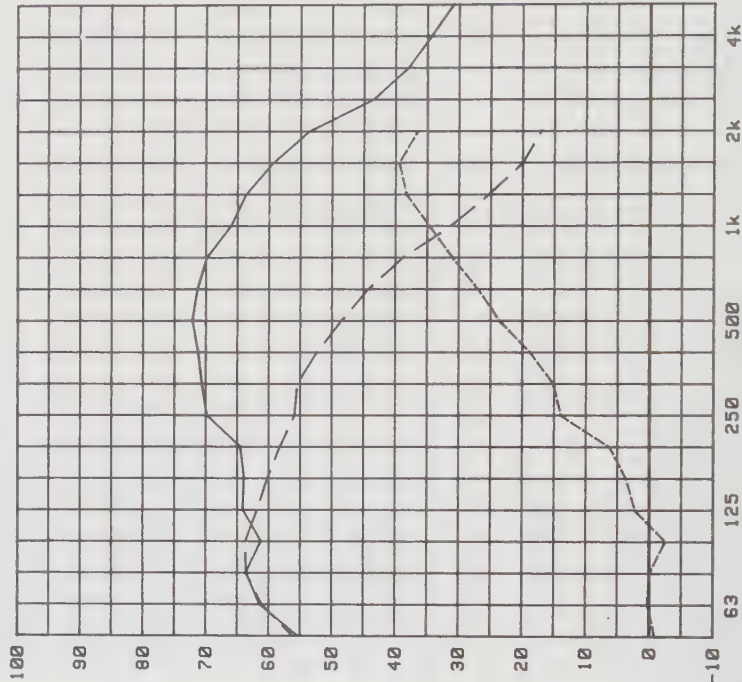
(1)

1.3 mm plastic carpet
22 mm chip-board
50 mm mineral wool type A
200 mm lightweight concrete



(2)

Difference (3)

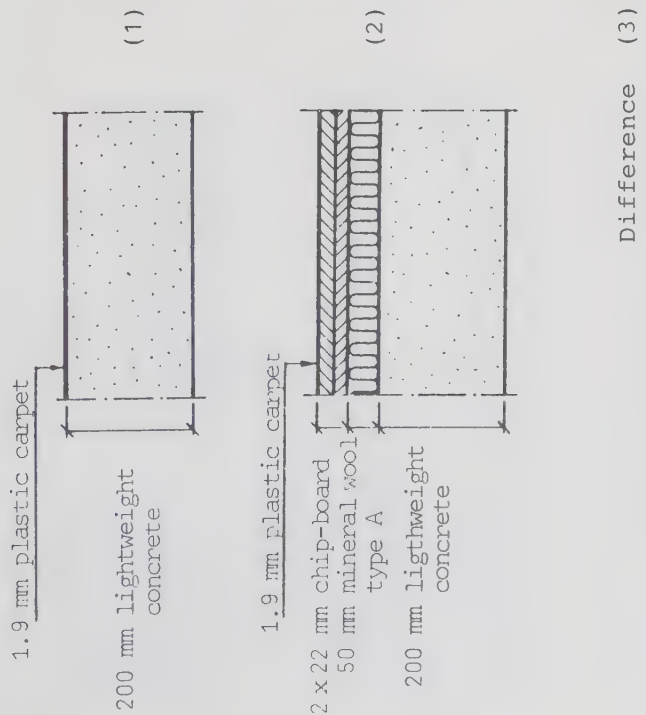
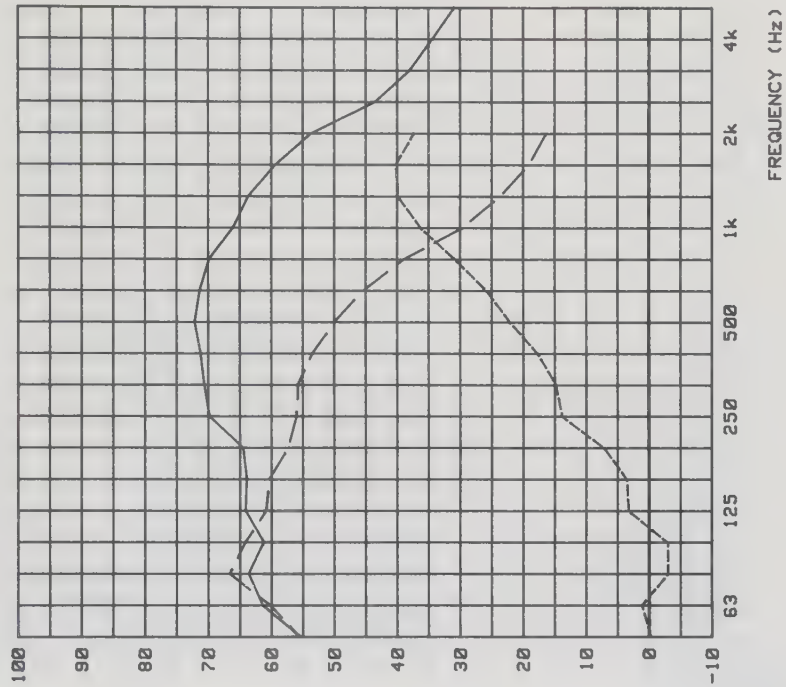


FREQUENCY (Hz)

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I f												
(1) ——— SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	71.2	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9	72
(2) ——— SL0052	55.6	60.2	66.6	64.1	60.8	60.3	57.3	56.0	55.8	53.5	50.0	45.5	39.0	29.7	23.7	19.1	16.3			59
(3) ----- d01502	-0.2	1.2	-3.0	-2.9	3.3	3.6	7.2	13.8	14.8	17.7	22.1	25.8	30.8	36.3	39.9	40.2	37.3			

NORMALIZED IMPACT LEVEL

L10 (dB)



	FREQUENCY (Hz)																						
	63	125	250	500	1k	2k	4k	11															
(1)	---	SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	70.6	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9
(2)	---	---	SLR054	53.7	58.7	66.1	63.1	59.0	58.6	55.5	55.0	54.8	53.2	50.2	45.5	39.2	29.8	22.5	17.6	14.5			
(3)	-----	delSR4	1.7	2.7	-2.5	-1.9	5.1	5.3	9.0	14.8	15.8	18.0	21.9	25.8	30.6	36.2	41.1	41.7	39.1				

NORMALIZED IMPACT LEVEL

1.9 mm plastic carpet

200 mm lightweight concrete

(1)

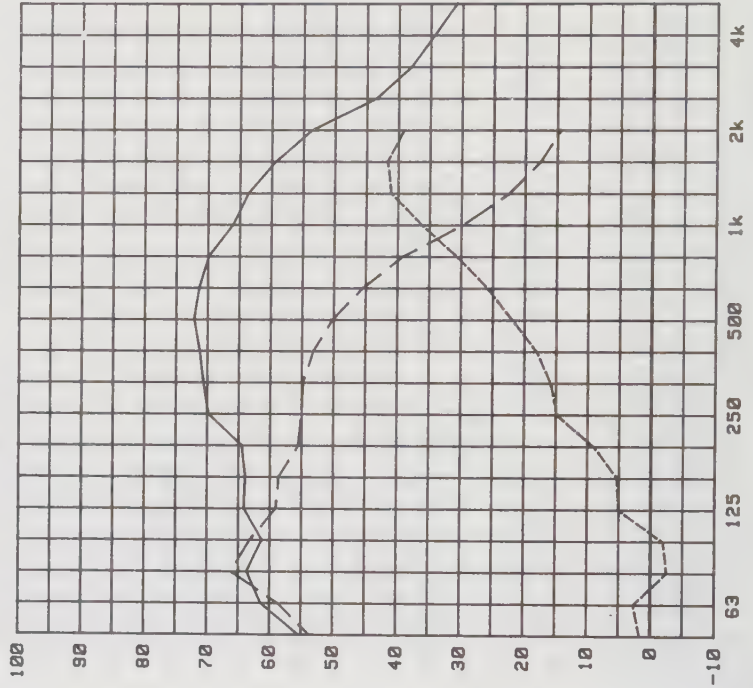
1.9 mm plastic carpet

4 x 22 mm chip-board
50 mm mineralwool type A
200 mm lightweight concrete

(2)

Difference (3)

L10 (dB)

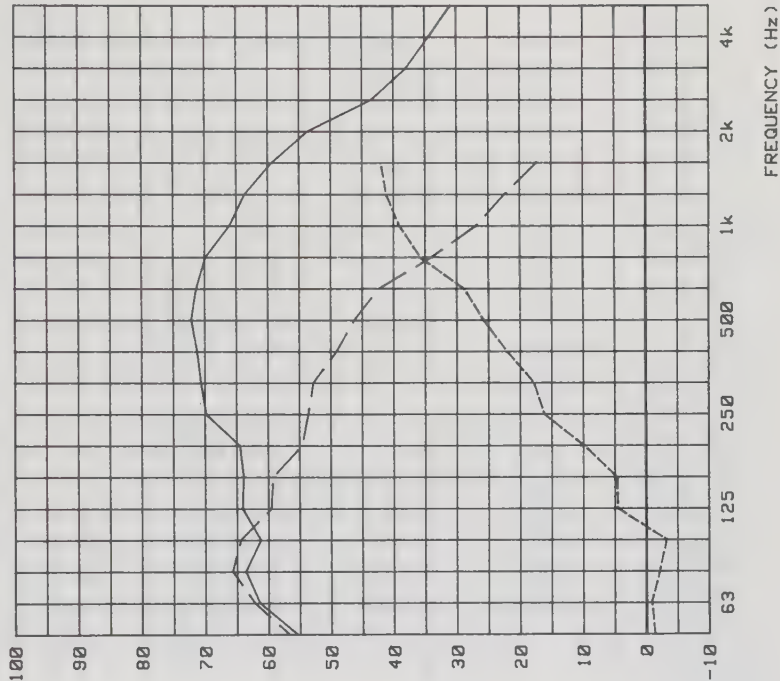


FREQUENCY (Hz)

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i														
(1) ----- SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	70.6	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9	72
(2) ----- SLg051	56.7	62.2	65.7	64.4	59.5	59.2	54.6	53.6	52.8	49.1	46.3	42.4	34.0	26.9	22.4	17.3						59
(3) ----- delg51	-1.3	-0.8	-2.1	-3.2	4.6	4.7	9.9	16.2	17.8	22.1	25.8	28.9	35.8	39.1	41.2	42.0						

NORMALIZED IMPACT LEVEL

L10 (dB)

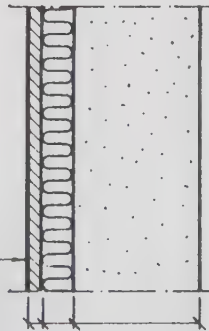


1.9 mm plastic carpet



200 mm lightweight
concrete

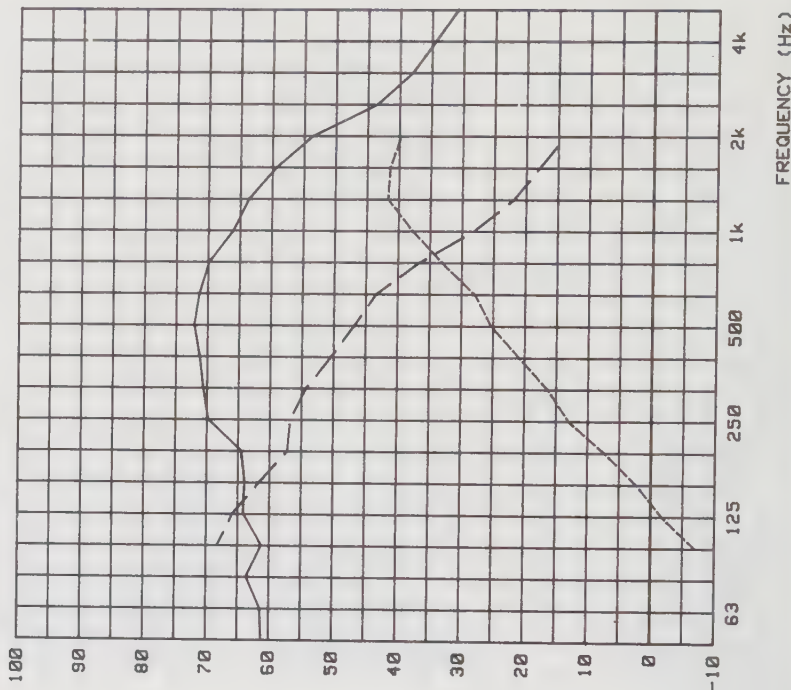
1.9 mm plastic carpet



22 mm chip-board
50 mm mineral wool
type g
200 mm lightweight
concrete

Difference (3)

NORMALIZED IMPACT LEVEL L10 (dB)



1.9 mm plastic carpet

200 mm lightweight concrete

1.9 mm plastic carpet

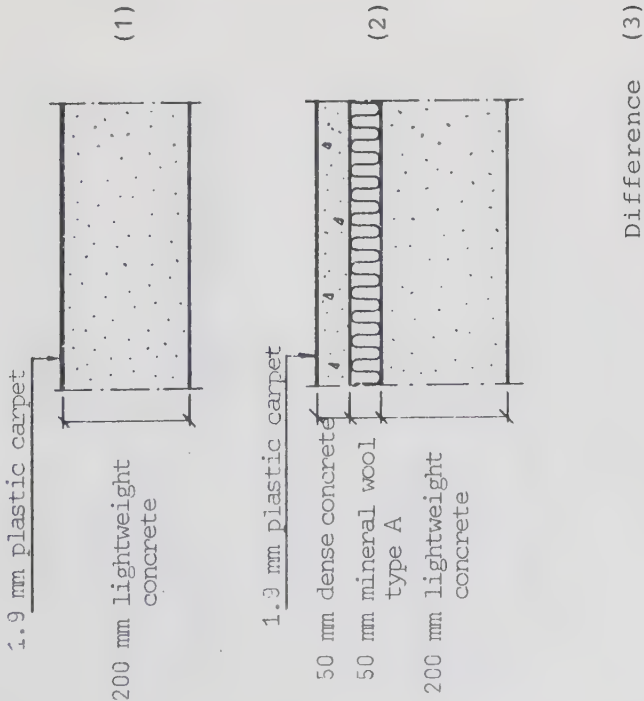
22 mm chip-board
20 mm foam-plastic
type d
200 mm lightweight concrete

Difference (3)

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i										
(1) ——— SLO000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9
(2) ——— SLO021	58.2	65.8	61.6	57.2	56.8	54.3	50.4	46.8	43.4	36.7	28.2	21.9	17.9	13.9				
(3) ——— d01Sd1	-7.0	-1.7	2.3	7.3	13.0	16.3	20.8	25.3	27.9	33.1	37.8	41.7	41.4	39.7				

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Ii	
(1) ———— SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	72
(2) ———— SLA05p	53.3	58.8	66.1	67.0	66.3	66.6	66.3	65.3	62
(3) ———— d01SAp	2.1	2.6	-2.5	-5.8	1.8	-2.4	6.1	11.4	

NORMALIZED IMPACT LEVEL
L10 (dB)



FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i													
(1) ——— SLO000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	70.6	71.2	72.1	71.3	69.8	65.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9
(2) ——— SLB05p	56.6	63.3	63.5	58.0	58.5	62.7	52.0	53.4	53.6	55.3	52.8	52.4	50.5	45.0	37.2	30.4	25.3	17.1	12.1		
(3) ——— delSBp	-1.2	-1.9	0.1	3.2	5.6	1.2	12.5	16.4	17.0	15.9	19.3	18.9	19.3	21.0	26.4	28.9	28.3	26.4	25.9		

NORMALIZED IMPACT LEVEL

1.9 mm plastic carpet

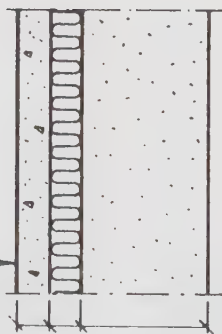
200 mm lightweight concrete



(1)

1.9 mm plastic carpet

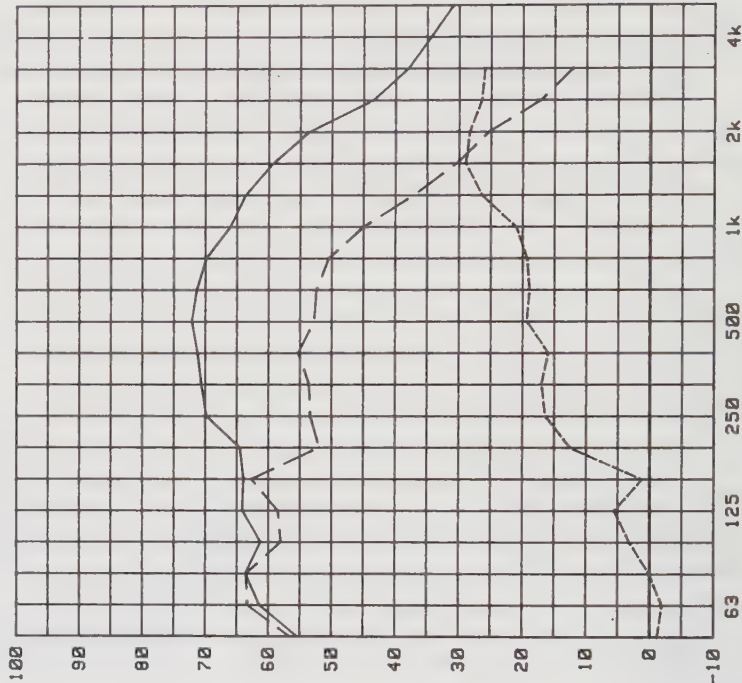
50 mm dense concrete
50 mm mineral wool type B
200 mm lightweight concrete



(2)

Difference (3)

L10 (dB)

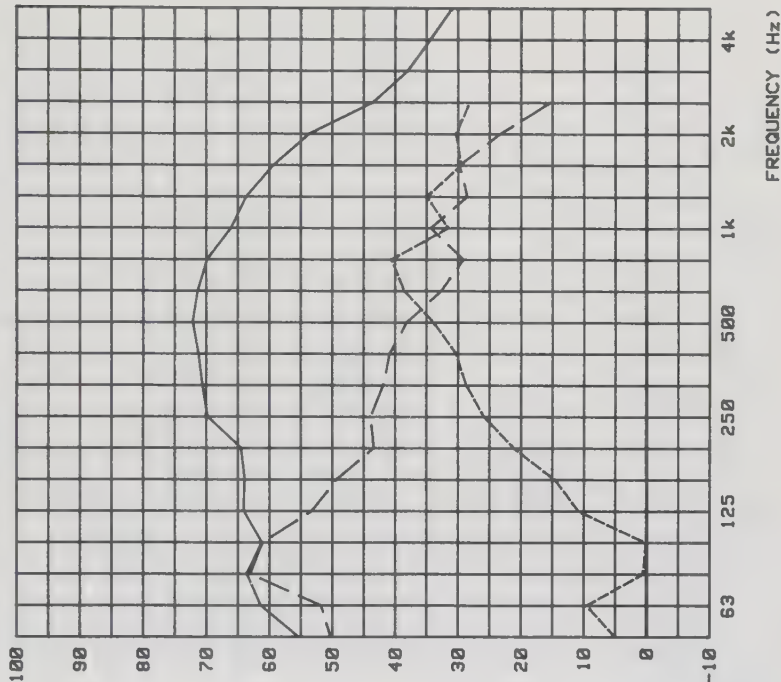


FREQUENCY (Hz)

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I1														
(1) ——— SL0000	55.4	61.4	63.6	61.2	64.1	63.9	64.5	69.8	70.6	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9	72
(2) ——— SLA05t	50.3	51.8	63.0	61.1	53.2	49.3	43.4	43.9	41.9	40.9	38.2	32.7	29.2	34.4	28.6	29.8	23.3	15.4				56
(3) ----- dSLA0t	5.1	9.6	0.6	0.1	10.9	14.6	21.1	25.9	28.7	30.3	33.9	38.6	40.6	31.6	35.0	29.5	30.3	28.1				

NORMALIZED IMPACT LEVEL

L10 (dB)



(1)

1.9 mm plastic carpet

200 mm lightweight concrete

(2)

1.9 mm plastic carpet

50 mm concrete tiles

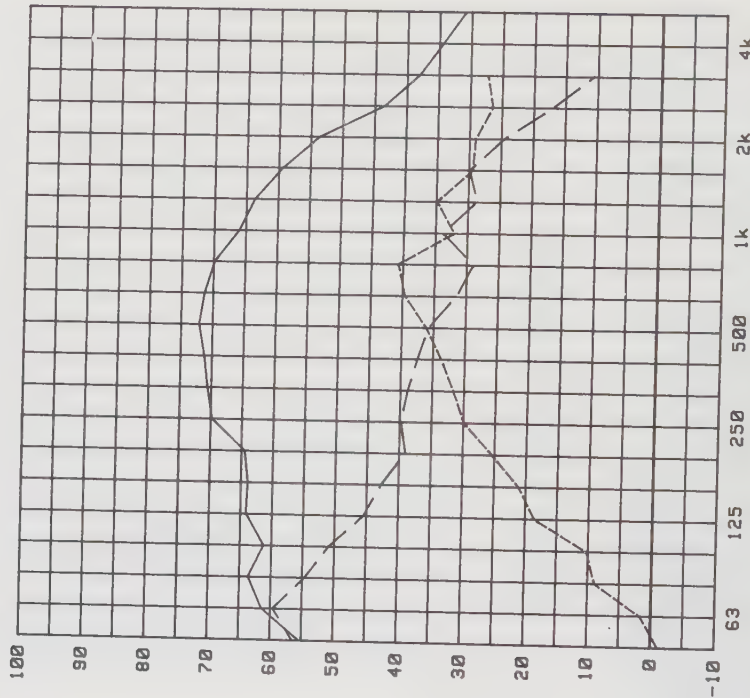
50 mm mineral wool type A

200 mm lightweight concrete

(3)

Difference

NORMALIZED IMPACT LEVEL L10 (dB)



(1)

(2)

Difference (3)

1.9 mm plastic carpet

200 mm lightweight concrete

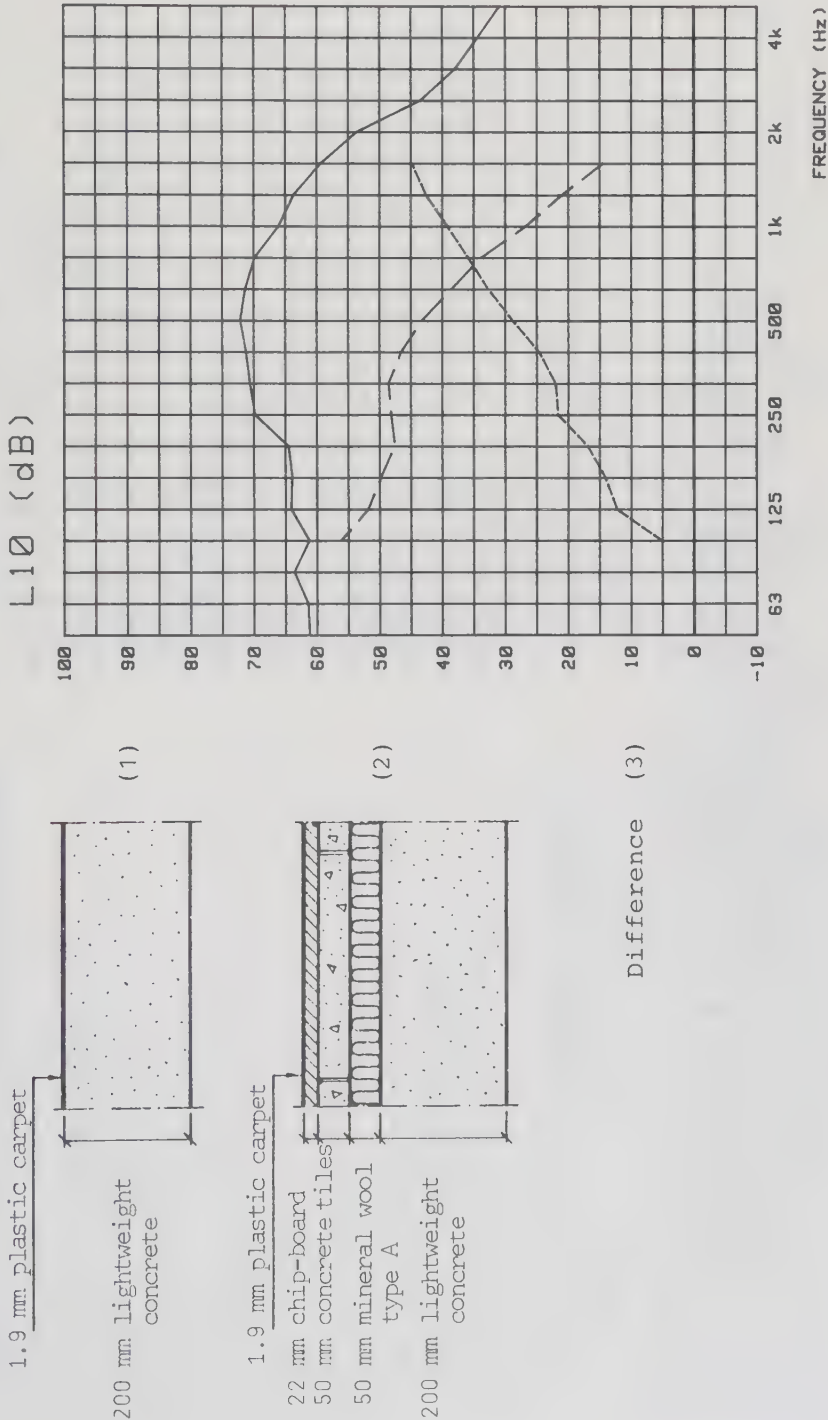
1.9 mm plastic carpet

50 mm concrete tiles
50 mm mineral wool type B
200 mm lightweight concrete

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	Ii										
(1)	——— SLO000	55.4	61.4	63.9	64.5	68.8	70.6	71.2	72.1	71.3	69.8	66.0	63.6	59.3	53.6	43.5	38.0	34.4	30.9
(2)	——— SLB05t	56.5	59.6	54.6	50.9	45.5	42.8	39.1	39.9	38.9	37.5	35.9	31.6	29.0	33.9	28.7	28.9	24.6	17.1
(3)	——— delSBt	-1.1	1.8	9.0	10.3	18.6	21.1	25.4	29.9	31.7	33.7	36.2	39.7	40.8	32.1	34.9	29.4	28.0	26.4

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Ii							
(1) ——— SLO000	55.4	61.2	64.5	69.8	70.6	71.2	72.1	71.3	66.0	59.3	43.5	38.0	34.4	30.9	72
(2) ——— SL005u		56.1	51.8	49.8	47.6	48.2	48.6	46.6	43.3	38.6	33.9	26.8	21.0	14.5	51
(3) ——— del05u		5.1	12.3	14.1	16.9	21.6	22.0	24.6	28.6	32.7	35.9	39.2	42.6	44.8	

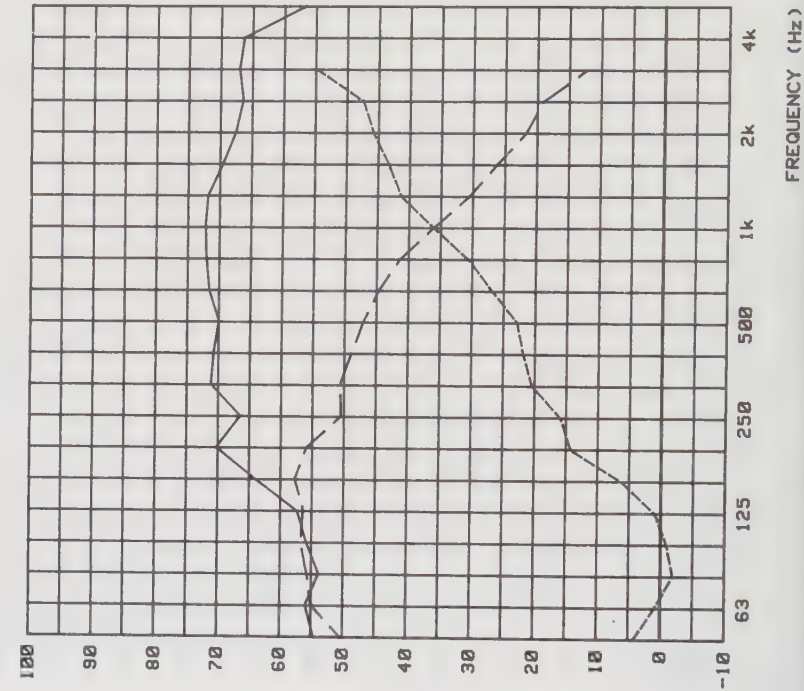
NORMALIZED IMPACT LEVEL



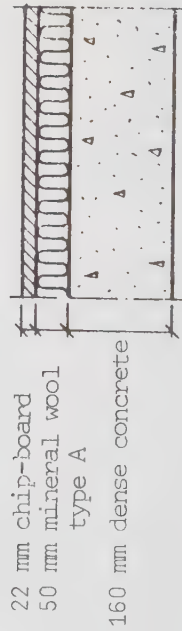
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i													
(1) ——— XTo000	54.6	55.9	53.8	55.8	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	71.8	69.5	67.5	66.4	67.0	66.3	66.6	82
(2) ——— XTR051	50.4	55.0	55.7	56.7	56.3	57.7	55.9	50.5	50.7	48.9	47.1	44.7	41.4	36.1	30.5	26.2	21.7	19.0	12.2		53
(3) ——— dXTR051	4.2	0.9	-1.9	-0.9	1.0	6.3	14.3	15.9	20.4	21.8	22.8	26.8	30.6	36.1	41.3	43.3	45.8	47.4	54.8		

NORMALIZED IMPACT LEVEL

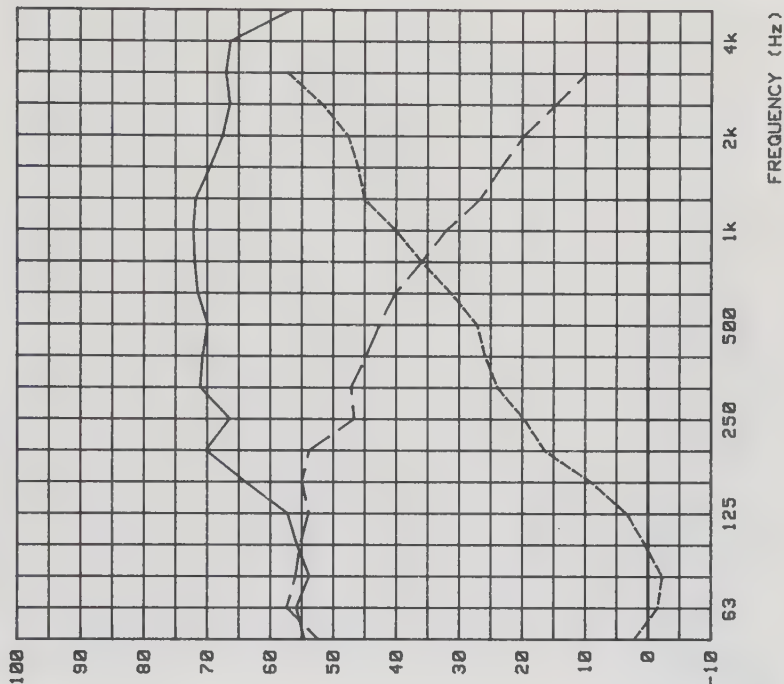
L10 (dB)



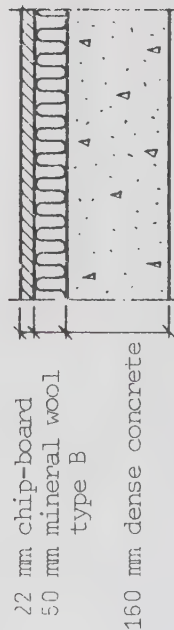
- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



NORMALIZED IMPACT LEVEL
L10 (dB)



- (1) L10 Structural slab only
(2) L10 Total construction
(3) ΔL Difference

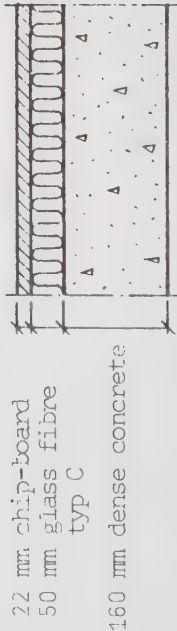
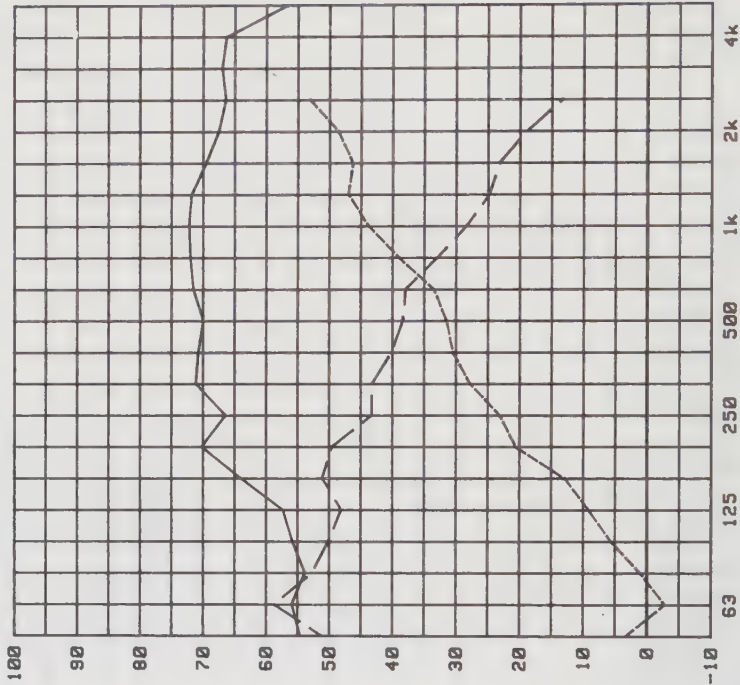


FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I1													
(1) ——— XTo000	54.6	55.9	53.8	55.8	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.8	69.5	66.4	67.0	66.3	56.6	82
(2) ——— XTC051	51.2	50.7	52.7	50.1	48.1	51.1	49.6	43.3	43.3	40.2	38.4	38.0	33.0	28.4	24.6	23.2	18.9	13.3			46
(3) - - - - - dXTC51	3.4	-2.8	1.1	5.7	9.2	12.9	20.6	23.1	27.8	30.5	31.5	33.5	39.0	43.8	47.0	46.3	48.6	53.1			

NORMALIZED IMPACT LEVEL

L10 (dB)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

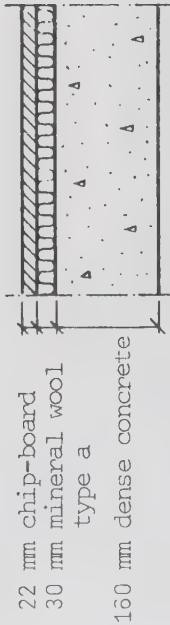
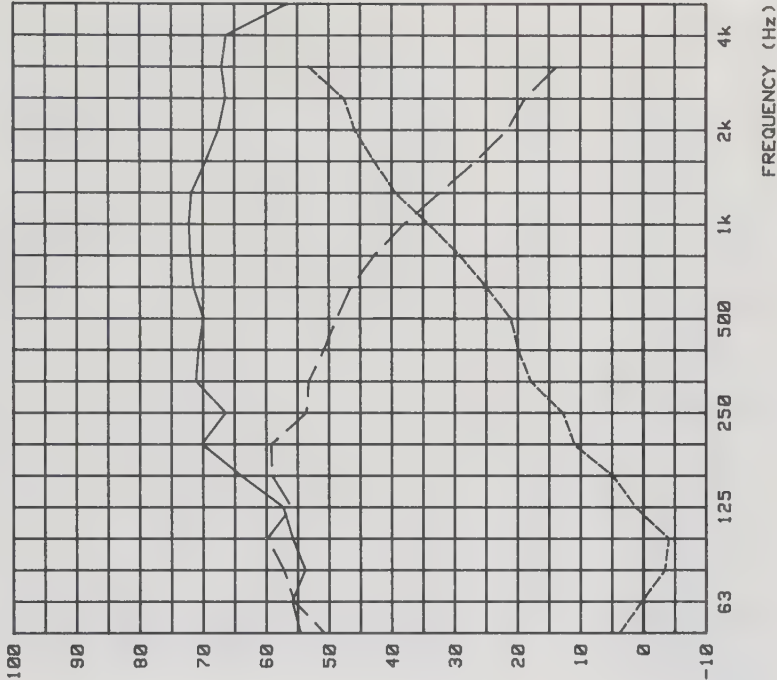


	FREQUENCY (Hz)											63	125	250	500	1k	2k	4k	Ii																	
(1)	----- XTo000											54.6	55.9	53.8	55.8	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.8	69.5	67.5	66.4	67.0	66.3	56.6	82			
(2)	----- XTa031											50.8	55.6	57.2	59.8	56.0	59.1	59.2	53.6	53.2	50.8	48.8	46.5	42.8	38.1	32.4	26.7	21.6	18.8	13.8						55
(3)	----- dXTa03											3.8	0.3	-3.4	-4.0	1.3	4.9	11.0	12.6	17.9	19.9	21.1	25.0	29.2	34.1	39.4	42.8	45.9	47.6	53.2						

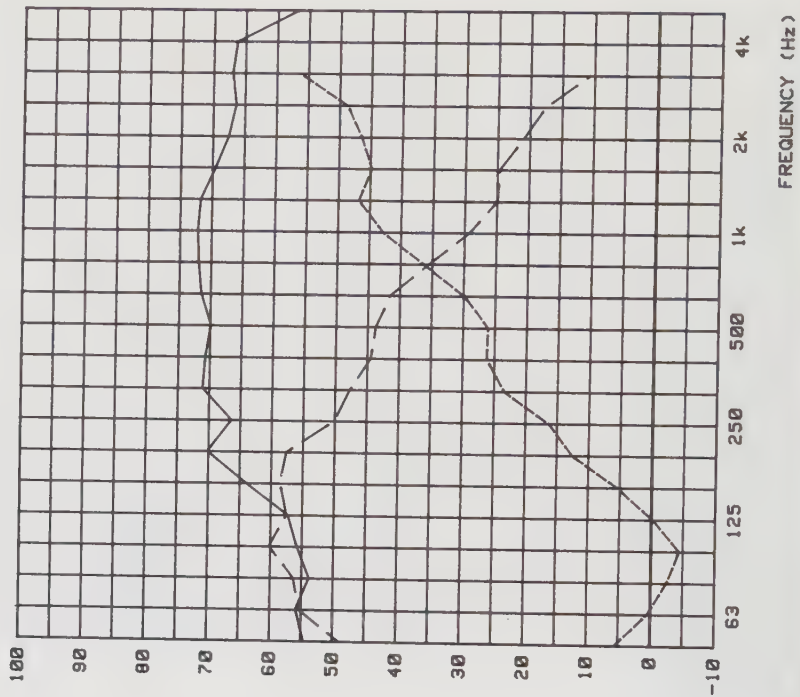
NORMALIZED IMPACT LEVEL

L10 (dB)

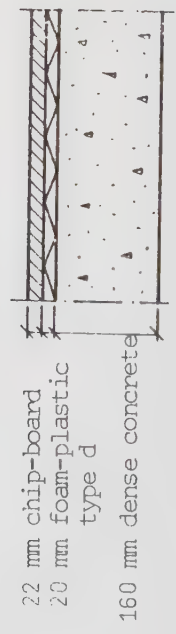
- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



NORMALIZED IMPACT LEVEL
L10 (dB)



- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

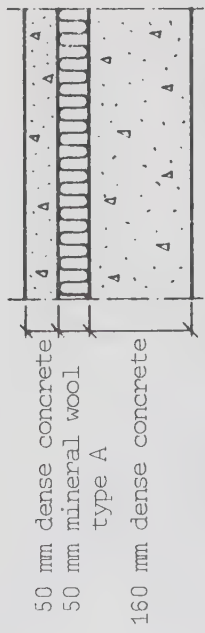
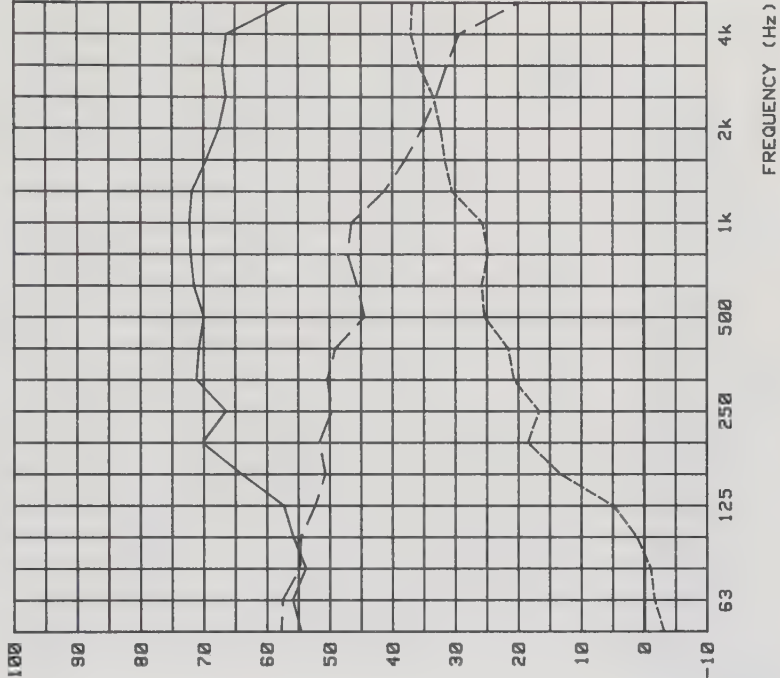


FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	I i														
(1) ----- XTo000	54.6	55.9	53.8	55.8	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.8	69.5	67.5	66.4	67.0	66.3	56.6	82
(2) ----- XTR05p	57.7	57.5	54.8	54.6	52.3	50.7	51.7	49.7	50.4	49.1	44.5	45.7	47.2	46.5	41.3	37.9	35.2	33.0	31.3	29.3	19.8	52
(3) ----- dXTR5p	-3.1	-1.6	-1.0	1.2	5.0	13.3	18.5	16.7	20.7	21.6	25.4	25.8	24.8	25.7	30.5	31.6	32.3	33.4	35.7	37.0	36.8	

NORMALIZED IMPACT LEVEL

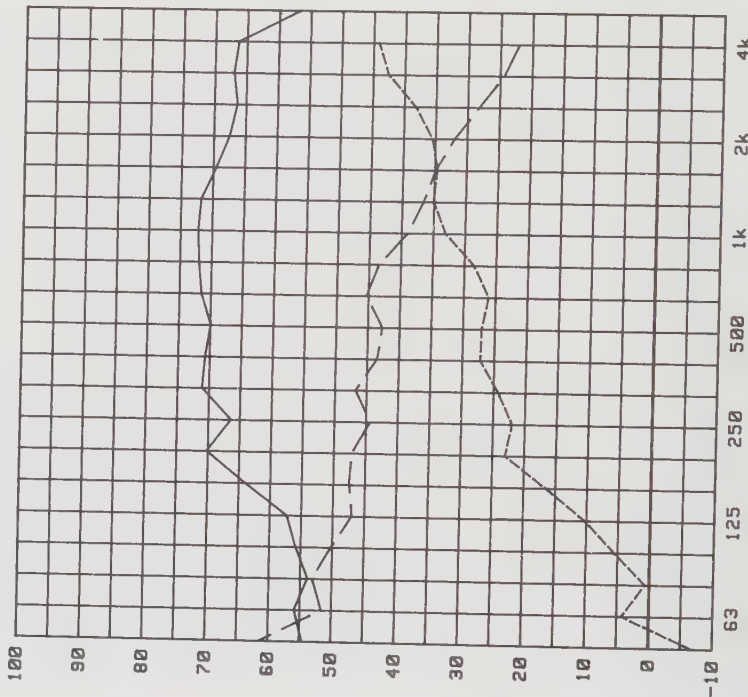
L10 (dB)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



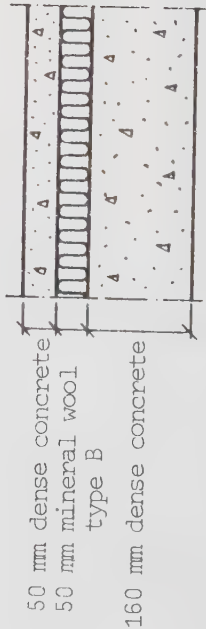
NORMALIZED IMPACT LEVEL

L10 (dB)



FREQUENCY (Hz)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

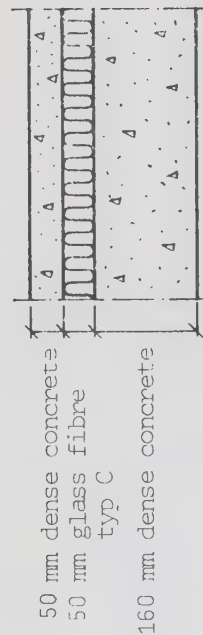
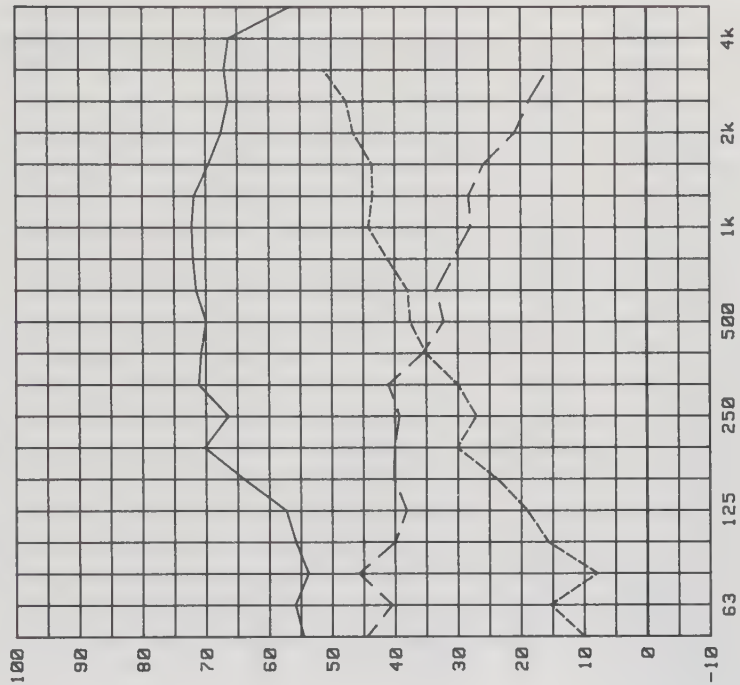


FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	Ii														
(1)	----- XT0000	54.6	55.8	53.0	55.0	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.0	69.5	67.5	66.4	67.0	66.3	66.6	82
(2)	----- XT005p	61.4	51.6	53.1	50.2	47.0	47.5	47.2	44.4	46.8	43.5	42.8	45.4	43.5	39.1	36.7	34.9	32.0	28.2	24.4	22.1		48
(3)	----- dXTB5p	-6.8	4.3	0.7	5.6	10.3	16.5	23.0	22.0	24.3	27.2	27.1	26.1	28.5	33.1	35.1	34.6	35.5	38.2	42.6	44.2		

NORMALIZED IMPACT LEVEL

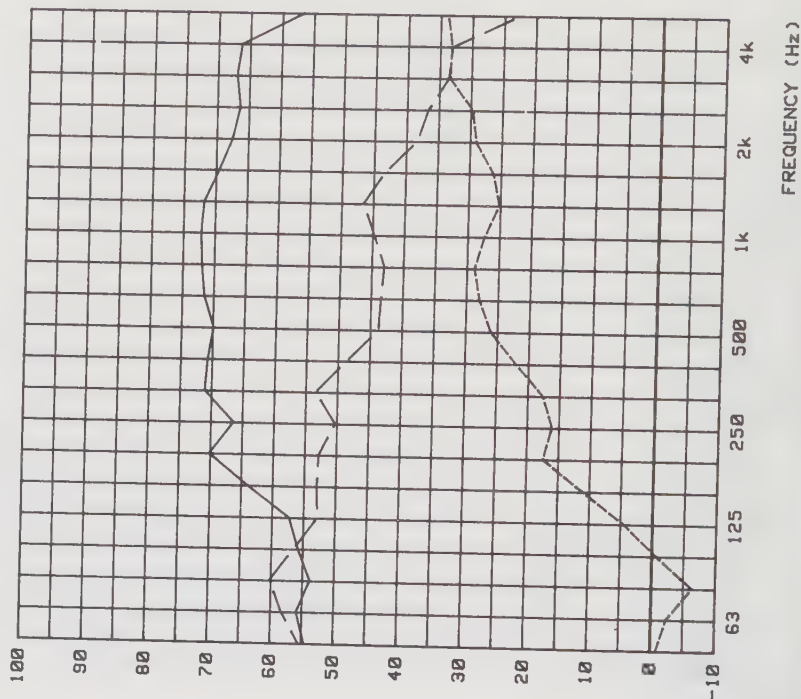
L10 (dB)

- (1) L10 Structural slab only
(2) L10 Total construction
(3) ΔL Difference

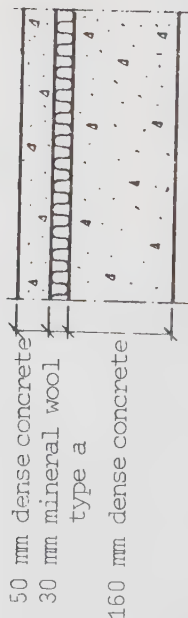


NORMALIZED IMPACT LEVEL

L_{10} (dB)

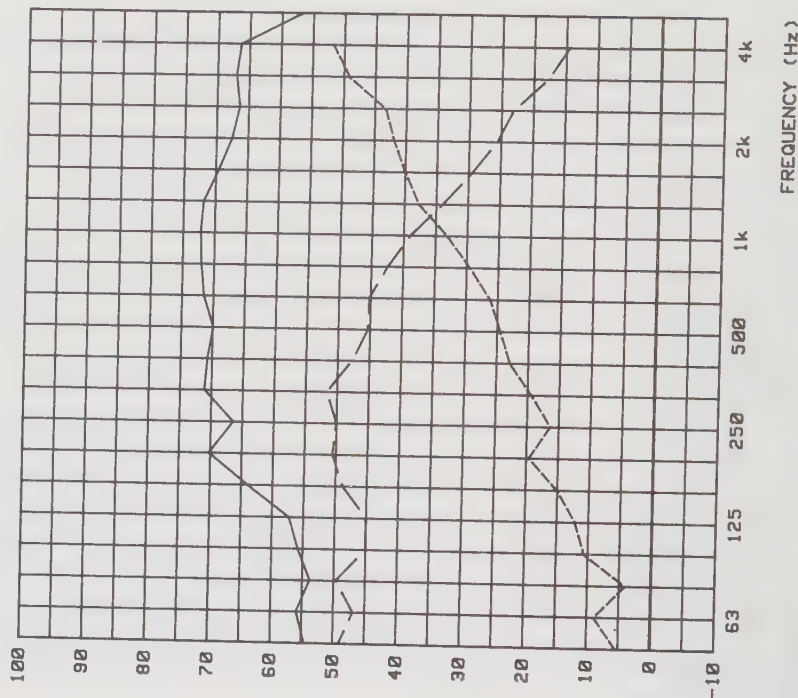


- (1) L_{10} Structural slab only
- (2) L_{10} Total construction
- (3) ΔL Difference

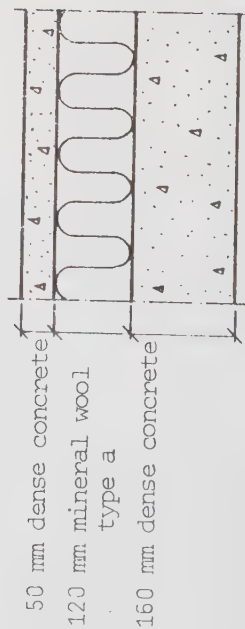


FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I i													
(1)	----- XTo000	54.6	55.9	53.8	55.0	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.2	71.8	69.5	67.5	66.4	67.0	66.3	56.6	82
(2)	----- XTa03p	55.5	58.3	60.2	56.5	52.8	53.0	52.8	53.5	48.7	43.8	43.5	43.1	44.0	46.6	43.3	38.4	36.7	33.6	33.1	22.7	54
(3)	----- dXa03p	-0.9	-2.4	-6.4	-0.7	4.5	11.0	17.4	16.1	17.6	22.0	26.1	28.0	28.9	27.4	25.2	26.2	29.1	29.7	33.4	33.2	33.9

NORMALIZED IMPACT LEVEL L10 (dB)



- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



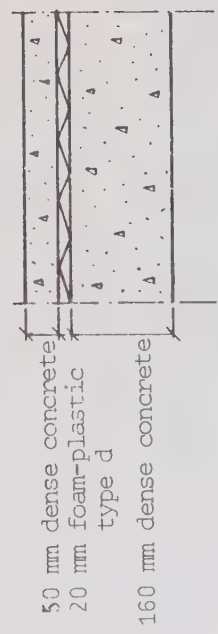
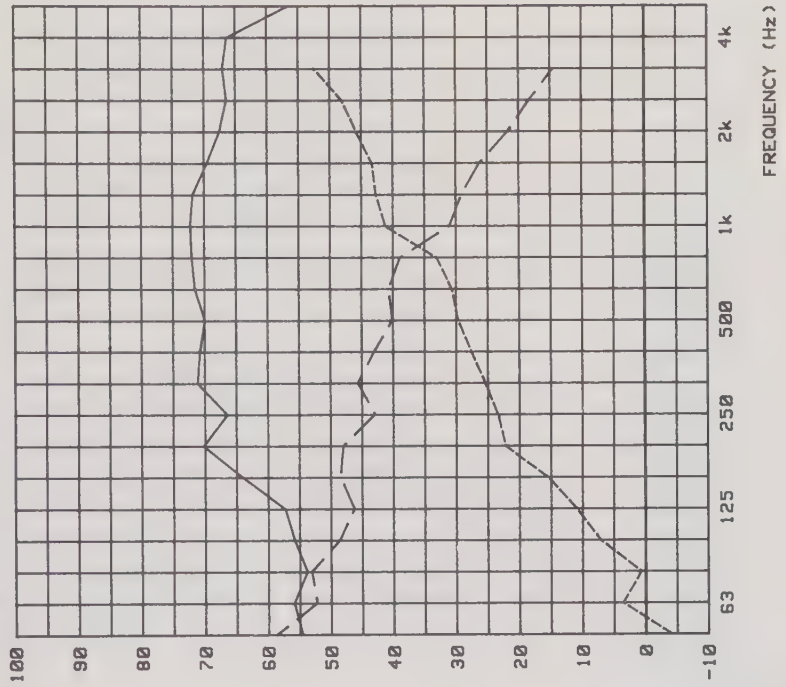
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I f														
(1)	----- XT0000	54.6	55.9	53.8	55.8	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.8	69.5	67.5	66.4	67.0	66.3	56.6	82
(2)	----- XT012p	49.1	46.9	49.7	45.1	45.1	48.9	50.6	50.1	51.7	47.8	45.4	45.3	42.5	39.0	34.0	29.5	25.6	23.1	17.8	14.5	49	
(3)	----- dX012p	5.5	9.0	4.1	10.7	12.2	15.1	19.6	16.3	19.4	22.9	24.5	26.2	29.5	33.2	37.8	40.0	41.9	43.3	49.2	51.8		

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I f												
(1)	— — — — — XT000	54.6	55.8	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.8	69.5	67.5	66.4	67.0	66.3	56.6	82			
(2)	— — — — — XTd0p	59.7	52.3	53.1	48.6	46.3	48.5	48.0	43.0	45.7	43.1	40.2	40.8	38.9	31.0	29.1	26.2	21.7	18.3	14.5	46
(3)	— — — — — dXTd2p	-4.1	3.6	0.7	7.2	11.0	15.5	22.2	23.4	25.4	27.6	29.7	30.7	33.1	41.2	42.7	43.3	45.8	48.1	52.5	

NORMALIZED IMPACT LEVEL

L10 (dB)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference

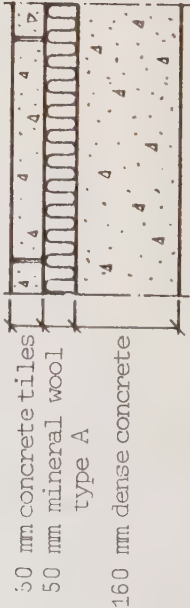
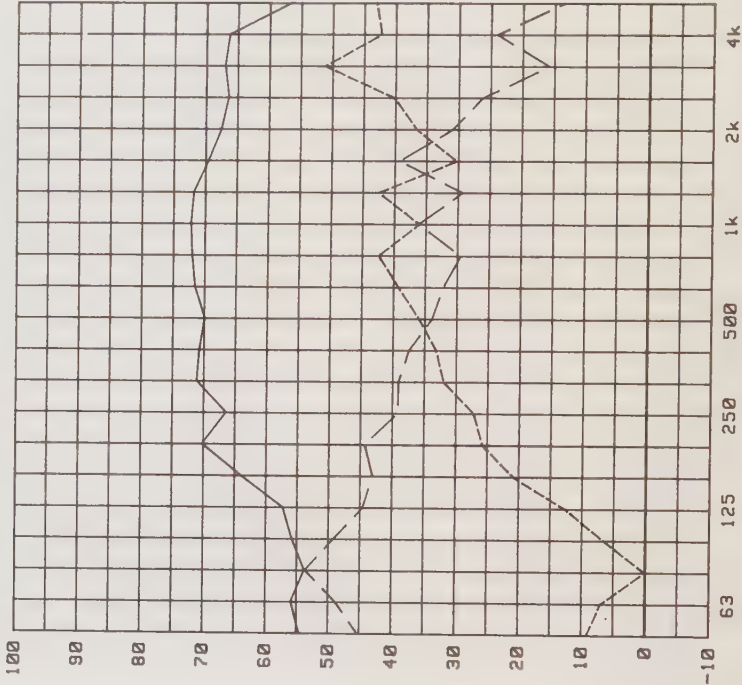


FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I i														
(1)	----- XTo000	54.6	55.9	53.8	55.8	57.3	64.0	70.2	66.4	71.1	70.7	69.9	71.5	72.0	72.2	71.8	69.5	67.5	66.4	67.0	66.3	56.6	82
(2)	--- -- XTR05t	45.4	48.9	53.7	49.2	44.6	43.1	44.4	39.2	39.1	37.5	33.9	32.2	29.5	36.2	29.3	39.3	30.9	26.2	15.8	24.0	13.5	45
(3)	----- dXTR5t	9.2	7.0	0.1	6.6	12.7	20.9	25.8	27.2	32.0	33.2	36.0	39.3	42.5	36.0	42.5	30.2	36.6	40.2	51.2	42.3	43.1	

NORMALIZED IMPACT LEVEL

L10 (dB)

- (1) L10 Structural slab only
- (2) L10 Total construction
- (3) ΔL Difference



APPENDIX II

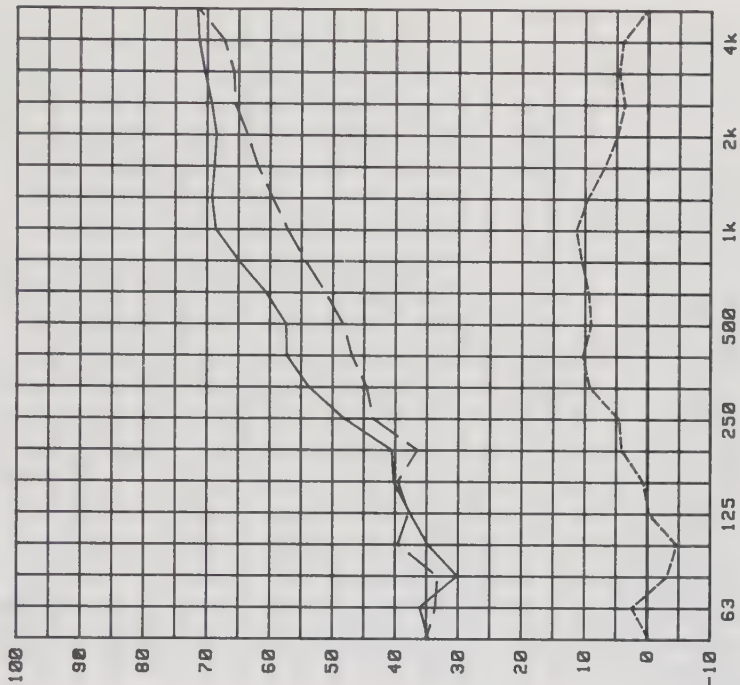
APPENDIX II Airborne sound insulation improvement

- II.1 Transmission loss measurements
 .1-.5 160 mm concrete structural slab
 .6-.7 200 mm lightweight structural slab
- | | FLOATING FLOOR | INTERLAYER |
|----|----------------|--------------------|
| .1 | chip board | 30 mm a |
| .2 | concrete slab | 50 mm A |
| .3 | concrete tiles | 50 mm A |
| .4 | chip board | 30 mm a reinforced |
| .5 | concrete slab | 30 mm A reinforced |
| .6 | chip board | 30 mm a |
| .7 | chip board | 30, 60, 120 mm a |
- II.2 Acceleration level improvement
 Structural slab: 160 mm dense concrete
- | | | |
|----|----------------|---------|
| .1 | chip board | 30 mm a |
| .2 | concrete slab | 50 mm A |
| .3 | concrete tiles | 50 mm A |
- II.3 Different measurements
- .1 Control measurements on flanking transmission
 - .2 Comparison measurement on the fastening of an accelerometer
 - .3 Transmission loss for the structural slabs theoretical and measured

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	Rm	Ia														
(1)	— RTa031	34.7	36.1	30.2	34.9	37.7	40.3	40.6	48.2	53.0	57.3	57.4	60.6	64.9	68.6	69.2	68.0	68.5	69.3	70.4	71.3	71.6	56.9	59
(2)	— RTa000	35.0	33.6	33.3	39.6	37.9	39.6	36.5	43.6	44.6	47.0	48.4	51.2	54.4	57.2	59.6	61.9	63.6	65.6	65.8	67.3	71.4	51.0	53
(3)	----- dRta03	-0.3	2.5	-3.1	-4.7	-0.2	0.7	4.1	4.6	9.2	10.3	9.0	9.4	10.5	11.4	9.6	6.9	4.9	3.7	4.6	4.0	0.2		

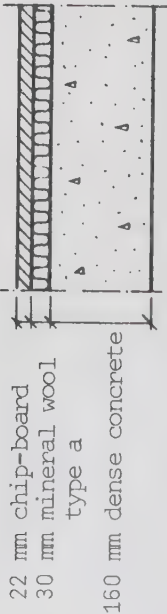
TRANSMISSION LOSS

R (dB)



FREQUENCY (Hz)

- (1) R Total construction with flanking transmission
- (2) R Structural slab only
- (3) ΔR Improvement reduced by flanking transmission

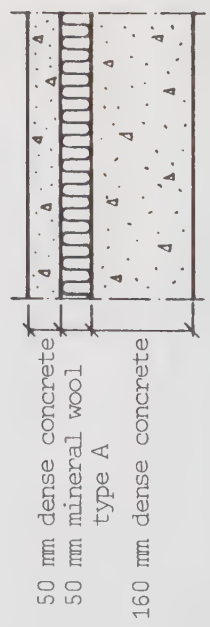
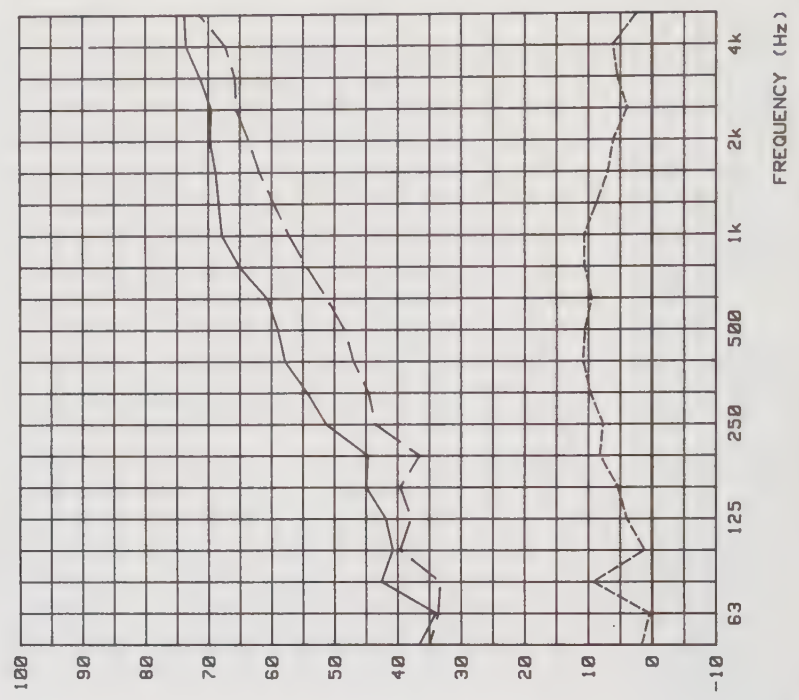


FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Rm	Ia														
(1) ———— RTR05p	36.6	34.0	42.6	40.8	41.9	45.0	44.7	51.3	54.3	57.8	58.9	60.6	65.0	67.8	68.3	69.7	69.5	71.3	73.8	58.5	61		
(2) ———— — RTR000	35.0	33.6	39.3	39.6	37.9	39.6	36.5	43.6	44.6	47.0	48.4	51.2	54.4	57.2	59.6	61.9	63.6	65.6	65.8	67.3	71.4	51.0	53
(3) - - - - - dRTA5p	1.6	0.4	9.3	1.2	4.0	5.4	8.2	7.7	9.7	10.8	10.5	9.4	10.6	10.6	8.7	6.9	6.1	3.9	5.5	6.2	2.4		

TRANSMISSION LOSS

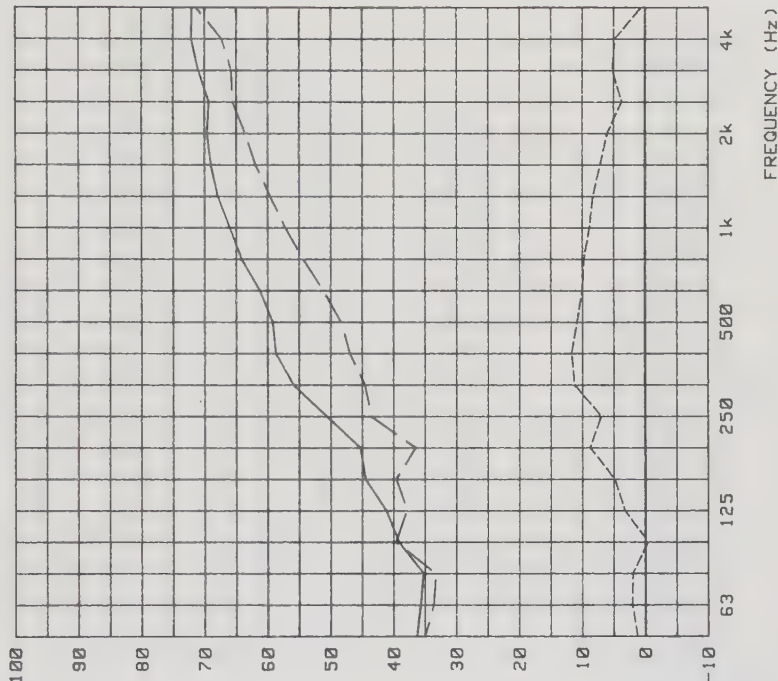
R (dB)

- (1) R Total construction with flanking transmission
- (2) R Structural slab only
- (3) ΔR Improvement reduced by flanking transmission

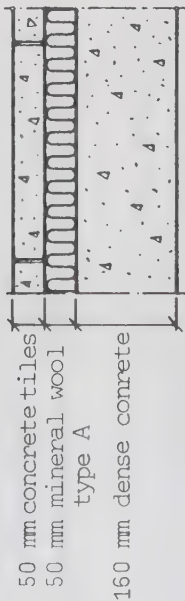


TRANSMISSION LOSS

R (dB)



- (1) R Total construction with flanking transmission
- (2) R Structural slab only
- (3) ΔR Improvement reduced by flanking transmission

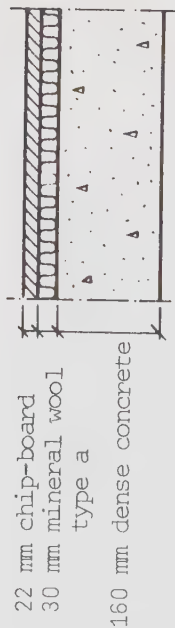
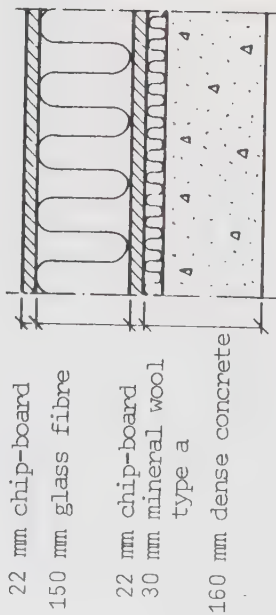
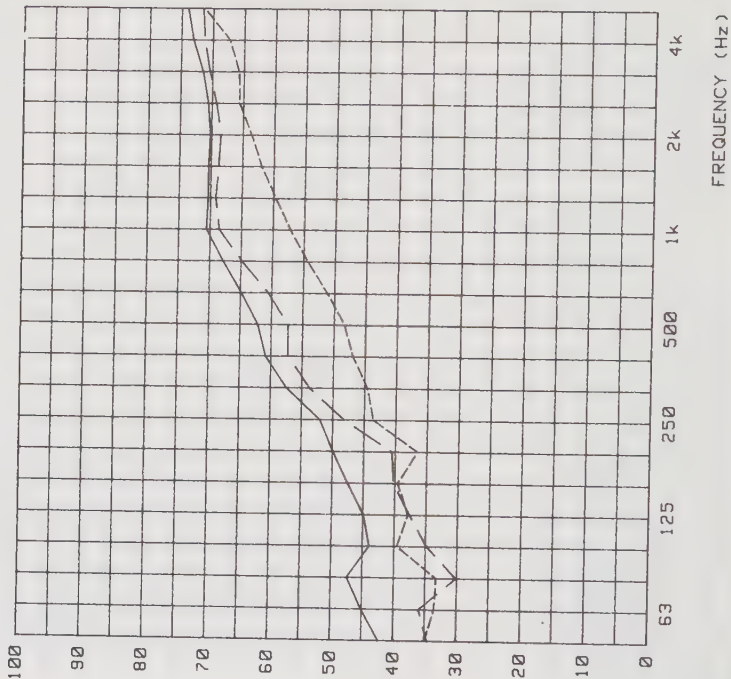


FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Rm	Ia														
(1) ———— RTA05t	36.3	35.7	35.3	39.2	41.2	44.5	45.3	50.6	55.9	58.7	59.2	61.2	64.1	66.0	67.9	69.0	69.0	69.3	71.0	72.1	72.0	58.3	61
(2) ———— RT0000	35.0	33.6	33.3	39.6	37.9	39.6	36.5	43.6	44.6	47.0	48.4	51.2	54.4	57.2	59.6	61.9	63.6	65.6	65.8	67.3	71.4	51.0	53
(3) ----- dRTA5t	1.3	2.1	2.0	-0.4	3.3	4.9	8.8	7.0	11.3	11.7	10.8	10.0	9.7	8.8	8.3	7.1	6.0	3.7	5.2	4.8	0.6		

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Rm	Ia														
(1) ——— RT _{max}	42.4	45.2	47.5	44.0	45.0	47.6	50.0	52.1	57.5	60.9	62.2	64.9	67.9	70.4	70.4	70.2	70.6	71.5	73.1	74.0	61.0	65	
(2) ——— RT ₀₃₁	34.7	36.1	30.2	34.9	37.7	40.3	40.6	48.2	53.8	57.3	57.4	60.6	64.9	68.6	69.2	68.8	68.5	69.3	70.4	71.3	71.6	56.9	59
(3) ——— RT ₀₀₀	35.0	33.6	33.3	39.6	37.9	39.6	36.5	43.6	44.6	47.0	48.4	51.2	54.4	57.2	59.6	61.9	63.6	65.6	65.8	67.3	71.4	51.0	53

TRANSMISSION LOSS

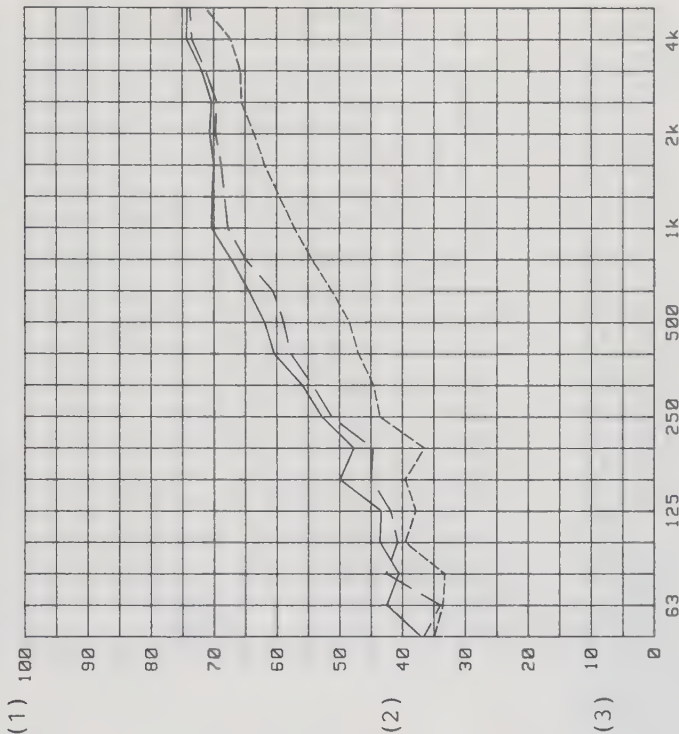
R (dB)



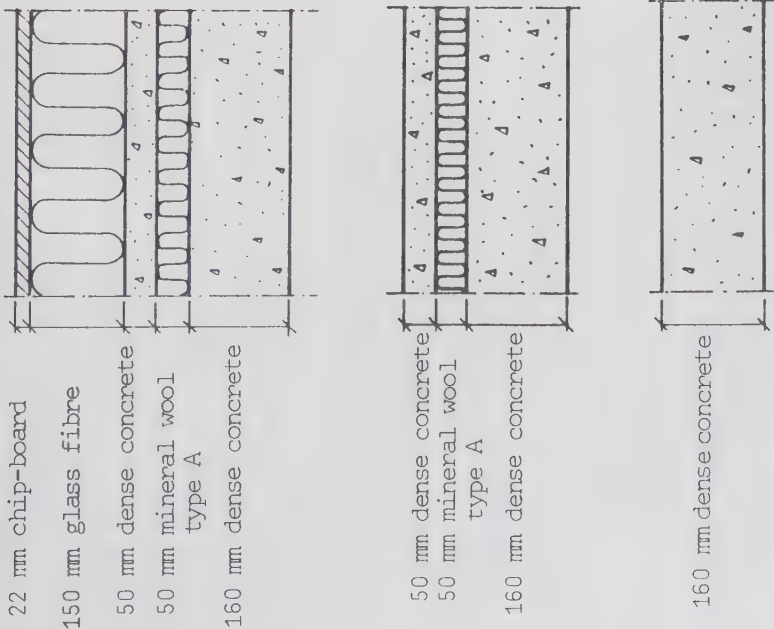
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Rm	Ia														
(1) ———— RTp _{pmx}	37.1	42.5	40.6	43.6	43.4	49.9	47.7	52.8	55.9	60.4	61.9	64.4	67.2	70.3	70.2	69.9	70.6	70.3	71.9	74.3	74.2	60.7	64
(2) ———— RTp _{05p}	36.6	34.0	42.6	40.8	41.9	45.0	44.7	51.3	54.3	57.8	58.9	60.6	65.0	67.8	68.3	68.8	69.7	69.5	71.3	73.5	73.8	58.5	61
(3) ———— RTp ₀₀₀	35.0	33.6	33.3	39.6	37.9	39.6	36.5	43.6	44.6	47.0	48.4	51.2	54.4	57.2	59.6	61.9	63.6	65.6	65.8	67.3	71.4	51.0	53

TRANSMISSION LOSS

R (dB)

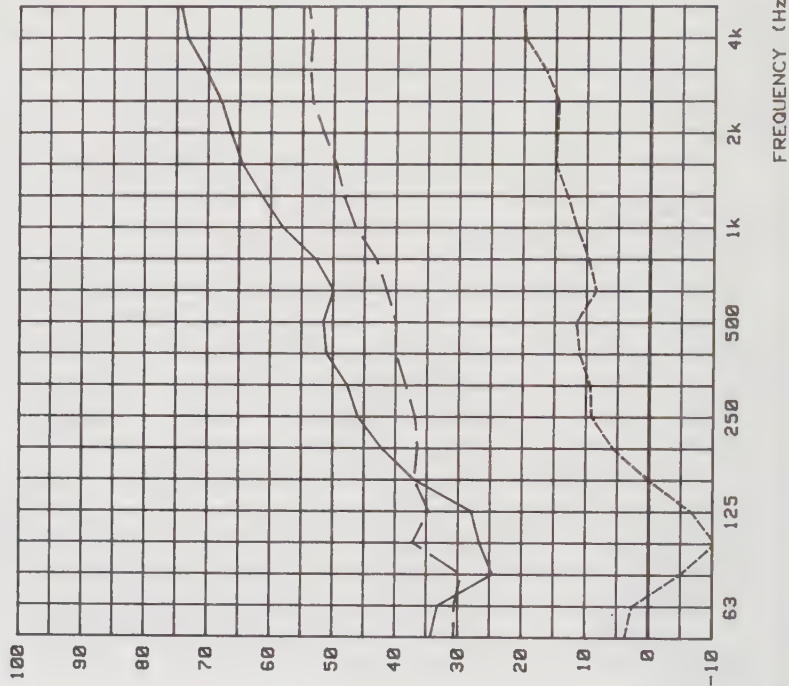


FREQUENCY (Hz)

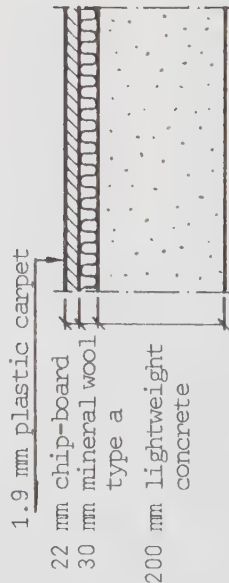


TRANSMISSION LOSS

R (dB)



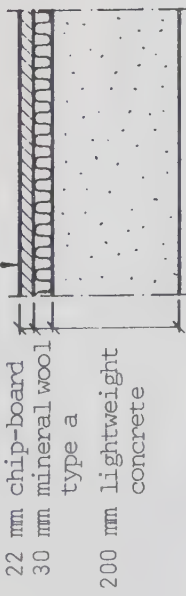
- (1) R Total construction with flanking transmission
- (2) R Structural slab only
- (3) ΔR Improvement, reduced by flanking transmission



FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	Rm	Ia														
(1)	— RL a031	34.3	33.2	24.6	26.7	28.0	37.0	42.2	46.0	47.7	51.0	51.5	49.9	52.0	58.0	61.4	64.5	66.3	67.9	70.4	73.3	74.3	51.3	52
(2)	— — — RL oref	30.6	30.6	29.5	37.2	34.7	37.0	36.5	36.9	38.4	40.0	40.0	41.5	43.2	46.5	48.4	49.6	51.6	53.4	53.8	53.5	54.1	43.1	46
(3)	----- dRL a03	3.7	2.6	-4.9	10.5	-6.7	0.0	5.7	9.1	9.3	11.0	11.5	8.4	9.6	11.5	13.0	14.9	14.7	14.5	16.6	19.8	20.2		

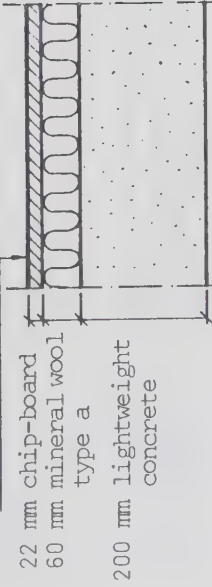
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	Rm	Ia														
(1) ——— RL031	34.3	33.2	24.6	26.7	28.0	37.0	42.2	46.0	47.7	51.0	51.5	49.9	52.8	58.0	61.4	64.5	66.3	67.9	70.4	73.3	74.3	51.3	52
(2) ——— RL061	30.9	30.4	22.2	28.6	34.8	41.5	44.1	45.2	49.4	54.4	56.8	59.2	62.2	64.8	67.5	69.2	70.0	70.2	71.9	74.3	75.9	55.6	56
(3) ——— RL121	22.4	22.9	31.5	37.6	38.1	42.7	45.3	47.6	51.4	55.5	58.9	62.1	65.2	68.4	69.7	70.8	70.9	70.2	71.3	70.2	69.5	57.9	59

1.9 mm plastic carpet



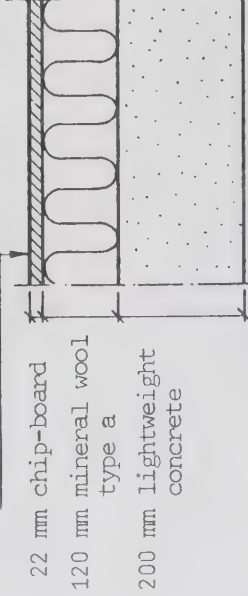
(1)

1.9 mm plastic carpet



(2)

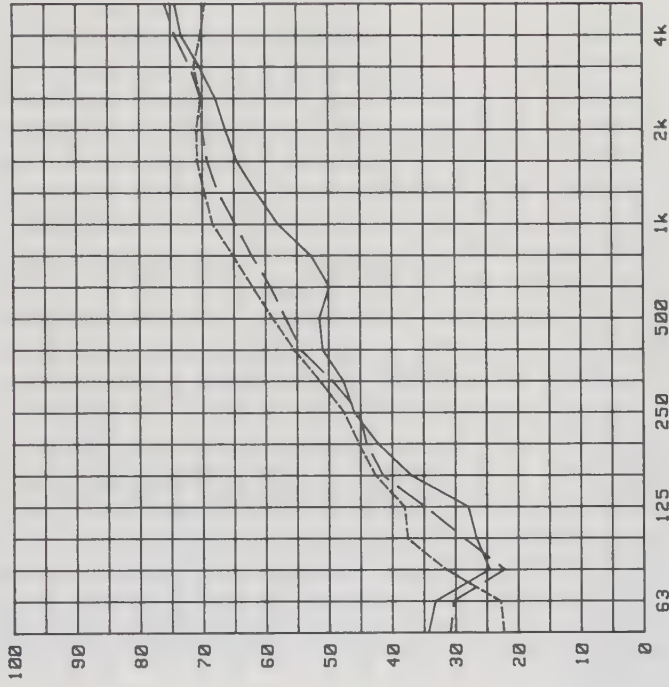
1.9 mm plastic carpet



(3)

TRANSMISSION LOSS

R (dB)

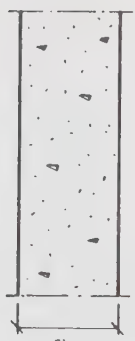
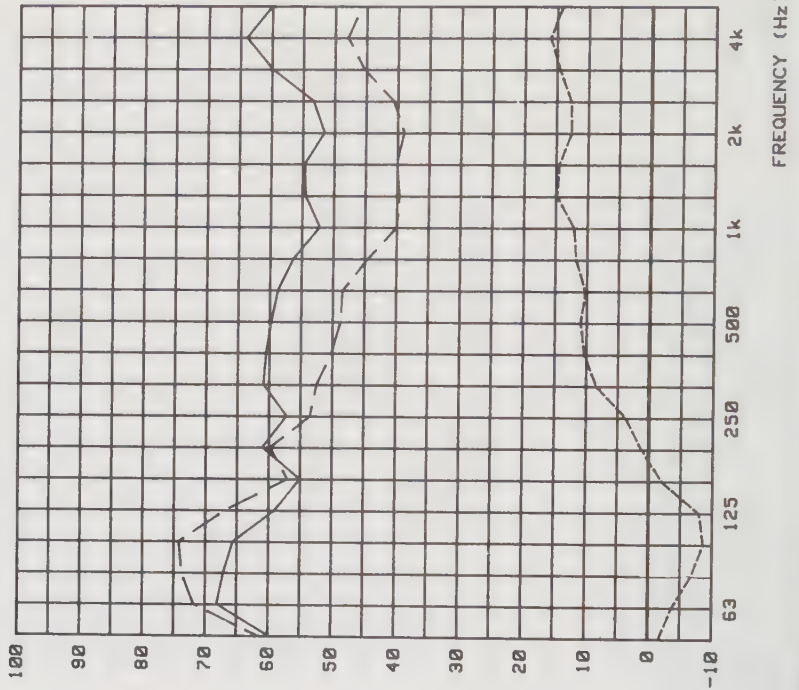


FREQUENCY (Hz)

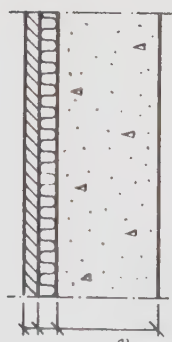
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	Rm														
(1)	LaTo00	59.8	68.1	67.1	65.6	59.0	55.0	61.0	57.2	60.9	60.5	59.7	58.7	56.3	52.2	54.5	54.7	51.6	53.4	59.9	64.0	60.0	0
(2)	LaTa03	61.5	71.8	73.8	74.2	66.9	57.0	59.8	53.5	52.5	50.2	48.8	46.5	44.6	40.1	39.7	40.2	39.0	40.7	45.4	48.0	45.9	0
(3)	dLaTa3	-1.7	-3.7	-6.7	-8.6	-7.9	-2.0	1.2	3.7	8.4	10.3	10.9	10.2	11.7	12.1	14.8	14.5	12.6	12.7	14.5	16.0	14.1	

ACCELERATION LEVEL

La+100 dB (re 1m/s^2)



160 mm dense concrete

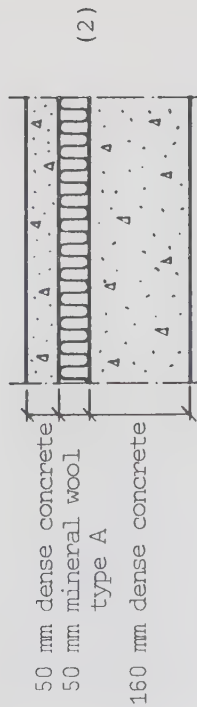
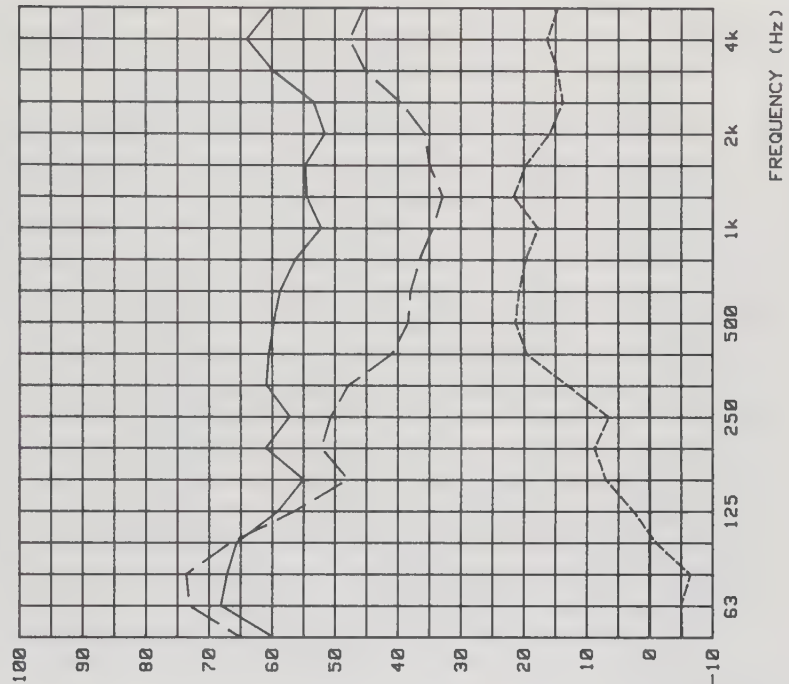


22 mm chip-board
30 mm mineral wool
type a
160 mm dense concrete

Difference (3)

ACCELERATION LEVEL

La+100 dB (re 1m/s^2)



(3)

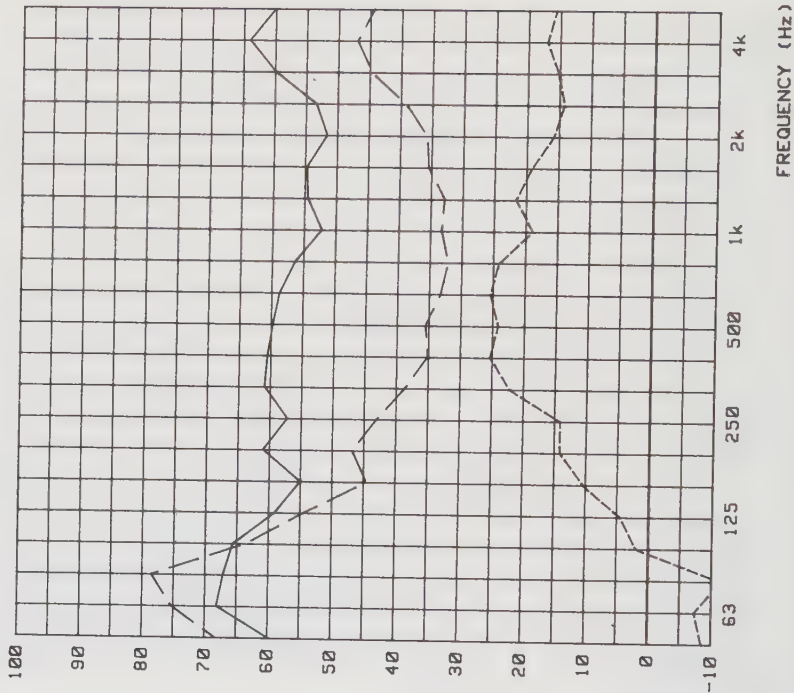
Difference

FREQUENCY (Hz)

	63	125	250	500	1k	2k	4k	I													
(1) — LaTo00	59.8	68.1	67.1	65.6	59.0	55.0	61.0	57.2	60.9	60.5	59.7	58.7	56.3	52.2	54.5	54.7	51.6	53.4	59.9	64.0	60.0
(2) — LaT95t	68.3	75.4	78.6	63.6	54.5	44.6	46.9	43.1	38.7	35.2	35.6	33.3	32.2	33.3	32.9	35.5	35.8	39.2	44.7	47.0	44.4
(3) — dLa95t	-8.5	-7.3	-11.5	2.0	4.5	10.4	14.1	14.1	22.2	25.3	24.1	25.4	24.1	18.9	21.6	19.2	15.8	14.2	15.2	17.0	15.6
										</											

ACCELERATION LEVEL

La+100 dB (re 1m/s²)



160 mm dense concrete



50 mm concrete tiles
50 mm mineral wool
type A
160 mm dense concrete

Difference (3)

II.3.2

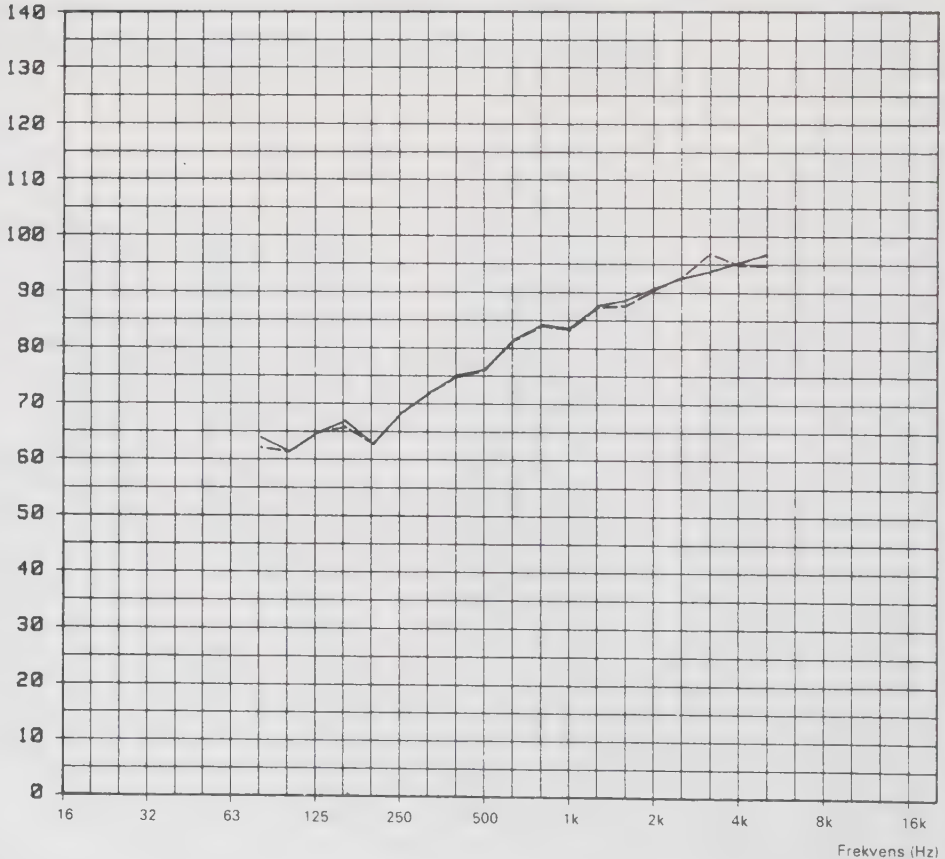
TEKNISKA HOGSKOLAN I LUND Byggnadsakustik		KURVBLAD NR MÄTDAG 830206 SIGN RM

Frekv	VAX	SKRUV
16		
20		
25		
32		
40		
50	Under	Under
63	Under	Under
80	63.5	61.8
100	61.0	61.0
125	64.4	64.4
160	66.5	65.4
200	62.4	62.4
250	67.9	67.9
315	71.5	71.5
400	74.6	74.4
500	75.7	75.6

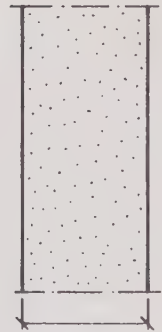
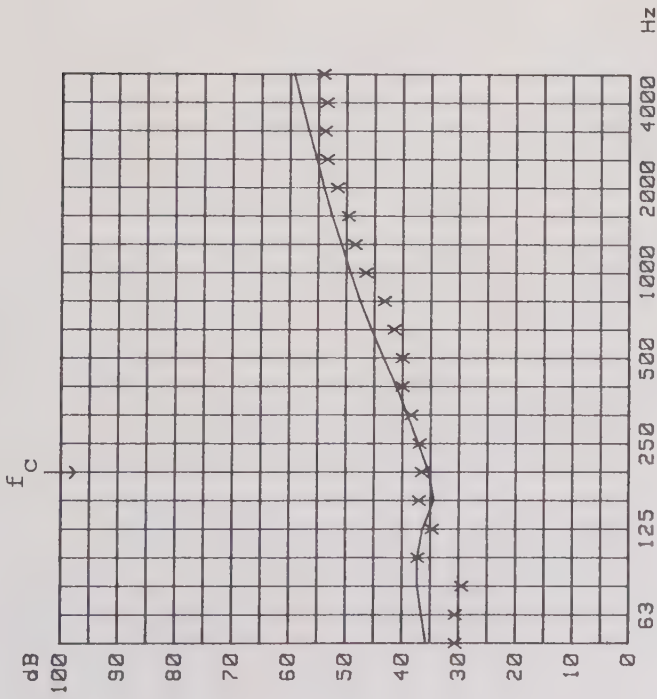
Frekv	VAX	SKRUV
630	80.9	80.7
800	83.6	83.4
1000	82.9	82.7
1250	87.0	86.7
1600	88.0	87.0
2000	90.1	89.8
2500	91.9	92.2
3150	93.1	96.3
4000	94.6	94.2
5000	96.1	94.1
6300		
8000		
10000		
12500		
16000		
20000		

P 0v SKRUV ----- Accelerometer fastened with a screw

P 0v VAX — Accelerometer fastened with bees wax
(dB)

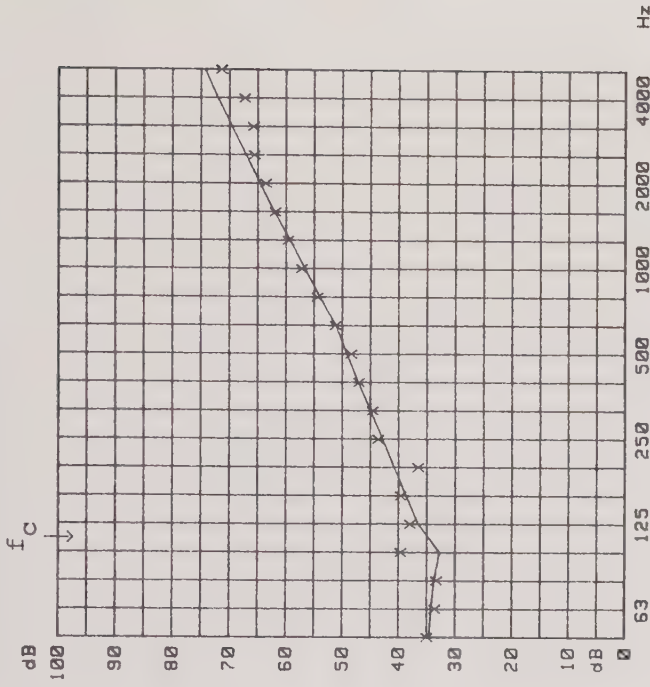


TRANSMISSION LOSS (R)



200 mm lightweight concrete

TRANSMISSION LOSS (R)



160 mm dense concrete

— calculated
x measured

APPENDIX III

APPENDIX III Comparison of the influence of
different parameters

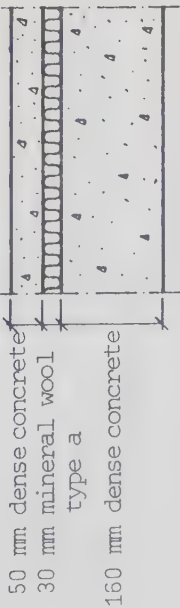
III.1	The width of the interlayer	
	FLOATING FLOOR	INTERLAYER
.1	concrete slab	a 30, 60, 120 mm
.2	chip board	a 30, 60, 120 mm
III.2	Different materials as interlayer	
.1	chip board	A, g
.2	concrete slab	A, B, C
.3	chip board	A, B, C
.4	concrete tiles	A, B
III.3	Different materials and different widths of the interlayer	
.1	concrete slab	A, d
.2	chip board	A, d
III.4	Number of chip board layers	
.1	single, double and quadruple chip board	A
III.5	Homogeneous concrete slab/concrete tiles as a floating floor	
.1	concrete slab/tiles	A concrete structural slab
.2	concrete slab/tiles	A lightweight structural slab
.3	concrete slab/tiles	B lightweight structural slab
III.6	Different structural slabs	
.1	concrete slab	A concrete/lightweight structural slabs
.2	concrete tiles	A concrete/lightweight structural slabs
.3	chip board	A concrete/lightweight structural slabs
.4	chip board	C concrete/lightweight structural slabs

- III.7 Influence of the thin plastic carpet
- .1 Improvement curves for the plastic carpet itself on lightweight concrete, chip board and concrete
 - .2 Improvement curves for the plastic carpet itself on concrete slab and concrete tiles
 - .3 Impact insulation improvement for a chip board floating floor with and without the plastic carpet
 - .4 Impact insulation improvement for a floating floor of concrete tiles with and without the plastic carpet
 - .5 Improvement curve for the plastic carpet itself on concrete tiles, measured as difference in force spectra

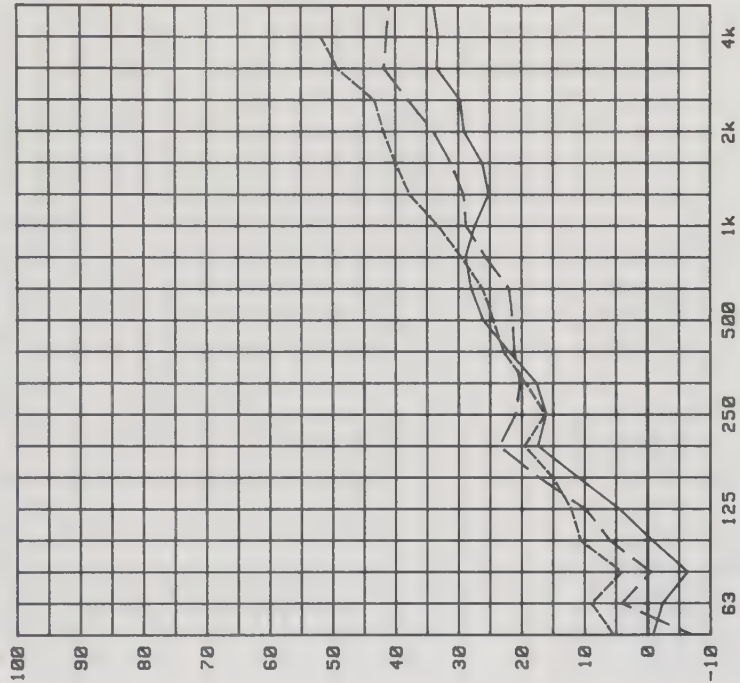
III.8 Impact level/airborne sound insulation improvement

		FLOATING FLOOR	INTER- LAYER	STRUCTURAL SLAB
.1	$\Delta R, \Delta L, \Delta L + \Delta F$	chip board	30 mm a	lightweight concrete
.2	$\Delta R, \Delta L, \Delta L_a$	concrete slab	50 mm A	concrete
.3	$\Delta R, \Delta L, \Delta L_a$	tiles	50 mm A	concrete
.4	$\Delta R, \Delta L, \Delta L_a$	chip board	30 mm a	concrete
.5	$\Delta L_a, \Delta L + \Delta F$	chip board	30 mm a	concrete
.6	ΔL_a	concrete slab/tiles	50 mm A	concrete

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	——— dXa03p	-0.9	-2.4	-6.4	-0.7	4.5	11.0	17.4	16.1	17.6	22.0	26.1	20.0	28.9	27.4	25.2	26.2	29.1	29.7	33.4	33.2	33.9
(2)	——— dXa06p	-7.1	4.1	-0.5	6.0	9.9	17.3	23.6	21.0	20.3	21.2	21.5	22.0	25.7	28.6	29.2	31.2	34.0	38.0	41.8	41.4	41.0
(3)	- - - - - dXa12p	5.5	9.0	4.1	10.7	12.2	15.1	19.6	16.3	19.4	22.9	24.5	26.2	29.5	33.2	37.6	40.0	41.9	43.3	49.2	51.8	



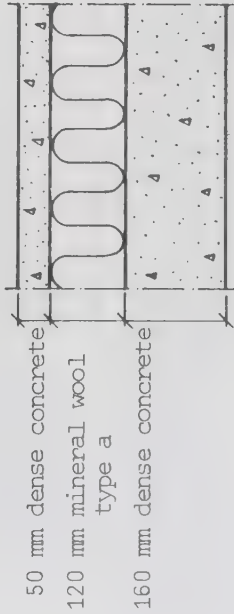
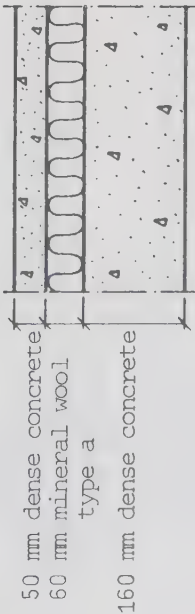
DELTA-L10 (dB)



(1)

(2)

(3)



FREQUENCY (Hz)		63	125	250	500	1k	2k	4k										
(1)	—	1.2	0.0	-3.1	-7.1	-0.4	2.1	5.0	12.6	14.8	19.1	23.5	25.6	30.6	35.3	39.7	40.7	37.3
(2)	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—
(3)	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—
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	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—
	—	—																

1.9 mm plastic carpet

22 mm chip-board

30 mm mineral wool

type a

200 mm lightweight

concrete

1.9 mm plastic carpet

22 mm chip-board

60 mm mineral wool

type a

200 mm lightweight

concrete

1.9 mm plastic carpet

22 mm chip-board

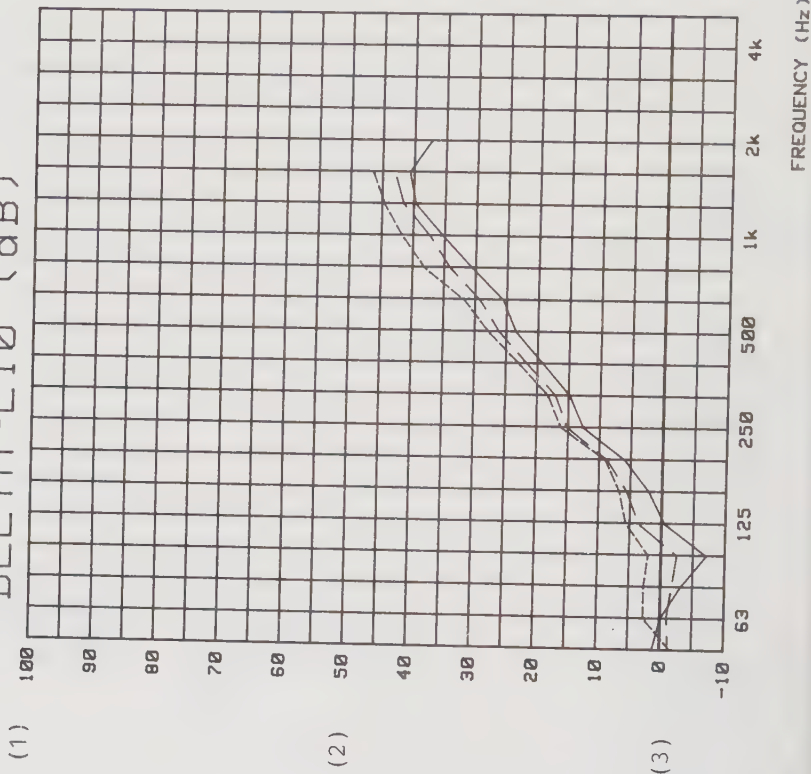
120 mm mineral wool

type a

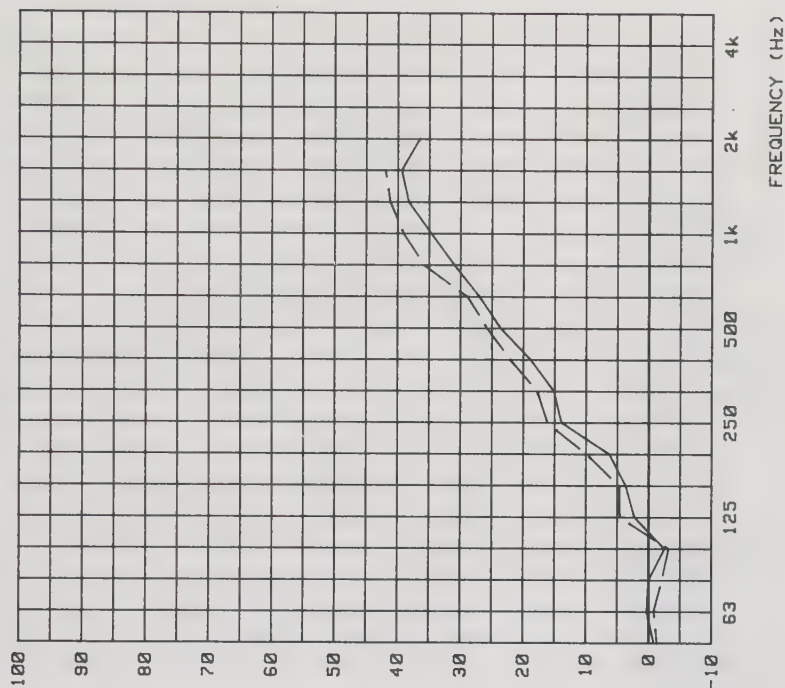
200 mm lightweight

concrete

DELTA-L10 (dB)



Note: The reference structural slab has the same plastic carpet as the chip board

[illegible]

1.9 mm plastic carpet

22 mm chip-board

50 mm mineral wool

type A

200 mm Lightweight

concrete

1.9 mm plastic carpet

22 mm chip-board

50 mm mineral wool

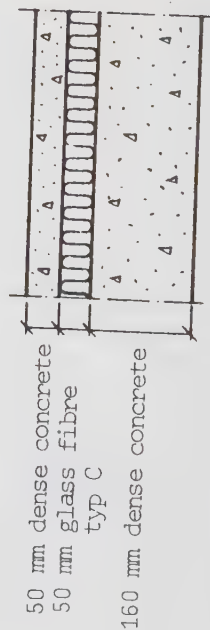
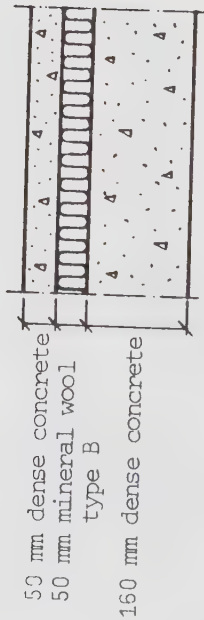
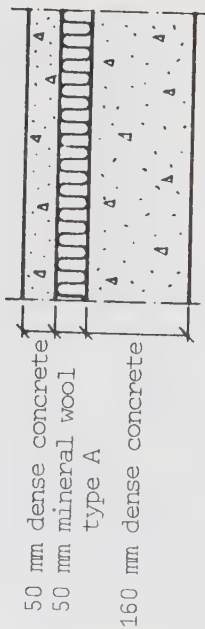
type b5

200 mm lightweight

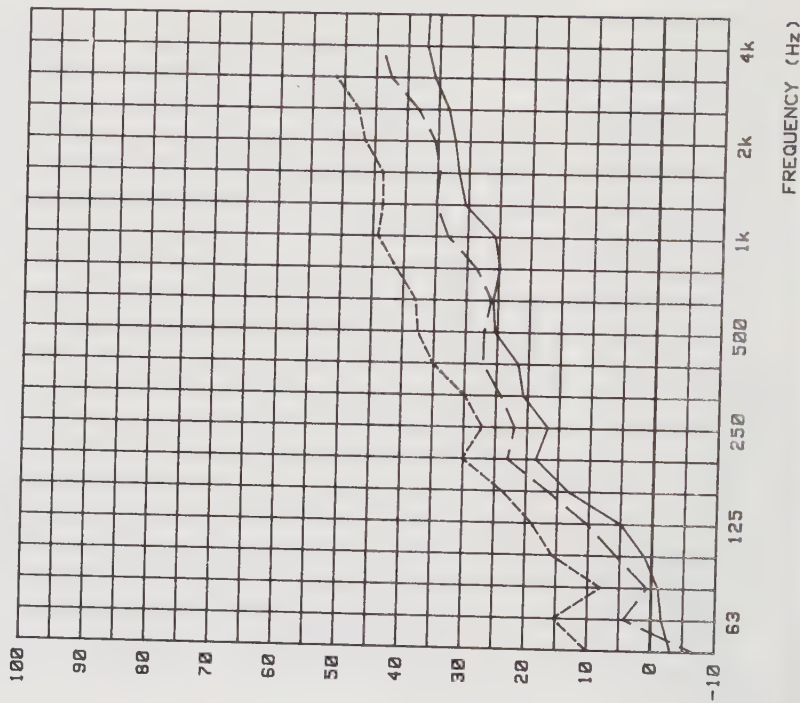
concrete

Note: The reference structural slab has the same plastic carpet as the chip board

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k													
(1)	----- dXTA5p	-3.1	-1.6	1.2	5.0	13.3	18.5	16.7	20.7	21.6	25.4	25.8	24.8	25.7	30.5	31.6	32.3	33.4	35.7	37.0	36.8
(2)	----- dXTB5p	-6.8	4.3	0.7	5.6	10.3	16.5	23.0	22.0	24.3	27.2	27.1	26.1	28.5	33.1	35.1	34.6	35.5	38.2	42.6	44.2
(3)	----- dXTC5p	10.1	15.4	8.0	15.8	19.1	23.8	30.1	27.1	30.0	35.1	37.6	38.0	41.2	44.2	43.5	43.6	46.6	47.7	51.3	

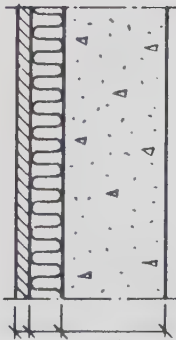


DELTA-L10 (dB)



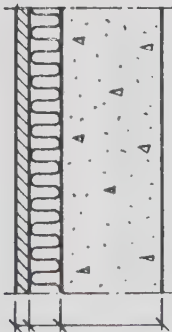
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k	
(1) ----- dXTB51	4.2	0.9 -1.9 -0.9	1.0	6.3 14.3 15.9	20.4 21.8 22.8	26.8 30.6 36.1	41.3 43.3 45.8	47.4 54.8
(2) ----- dXTB51	2.2 -1.5 -2.2	0.5 3.4	9.0 16.4 19.7	23.9 25.9 27.2	31.3 36.0 40.1	45.1 46.1 47.7	51.7 57.2	
(3) ----- dXTC51	3.4 -2.8	1.1 5.7	9.2 12.9 20.6	23.1 27.8 30.5	31.5 33.5 39.0	43.8 47.0 46.3	48.6 53.1	

22 mm chip-board
50 mm mineral wool
type A
160 mm dense concrete



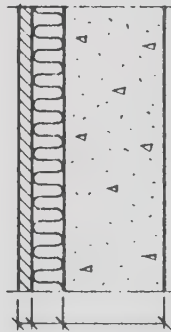
(1)

22 mm chip-board
50 mm mineral wool
type B
160 mm dense concrete



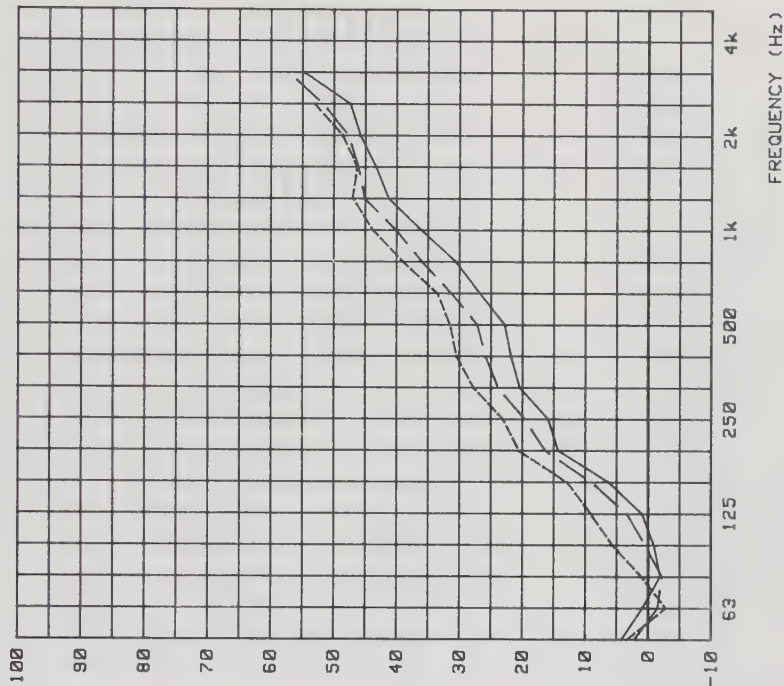
(2)

22 mm chip-board
50 mm glass fibre
type C
160 mm dense concrete



(3)

DELTA-L10 (dB)



FREQUENCY (Hz)

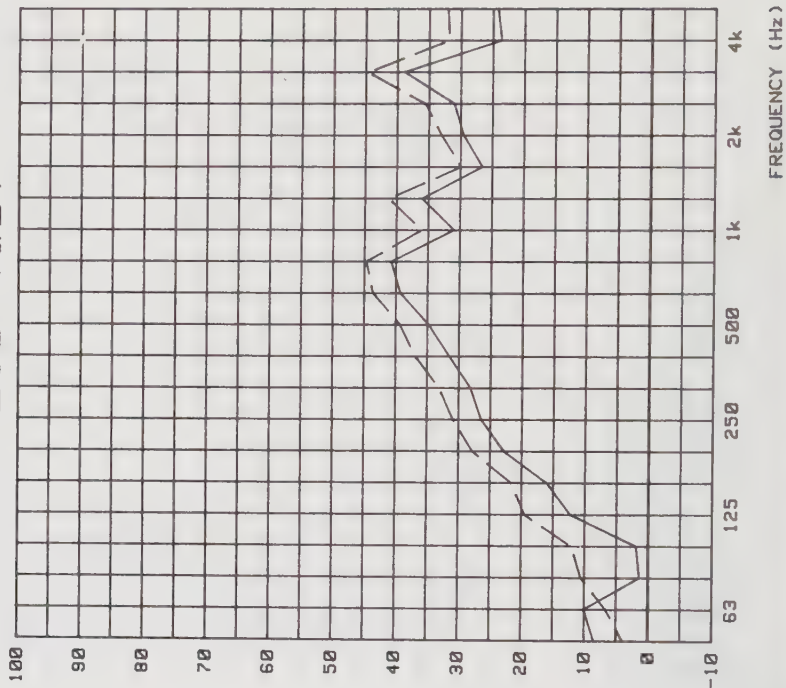
LTH

AKUSTIK

III.2.4

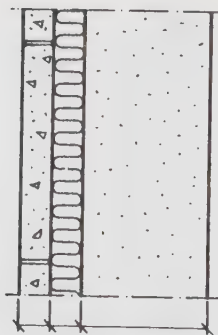
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	delXBt	8.5	10.1	1.4	1.9	12.3	16.0	22.8	26.4	28.1	31.5	34.8	39.3	40.8	30.9	36.0	26.5	29.5	31.0	38.9	23.6	24.2
(2)	delXBt	4.0	6.7	10.6	12.1	19.5	21.6	27.0	30.9	33.1	36.9	39.3	43.6	44.7	36.0	41.2	29.9	33.0	35.6	44.6	31.9	32.1

DELTA-L10 (dB)

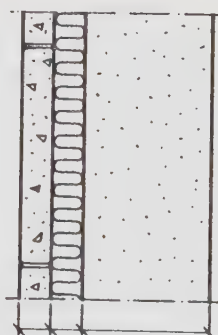


(1)

(2)



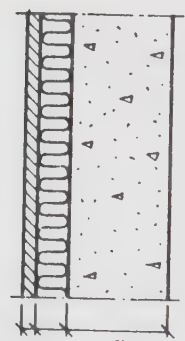
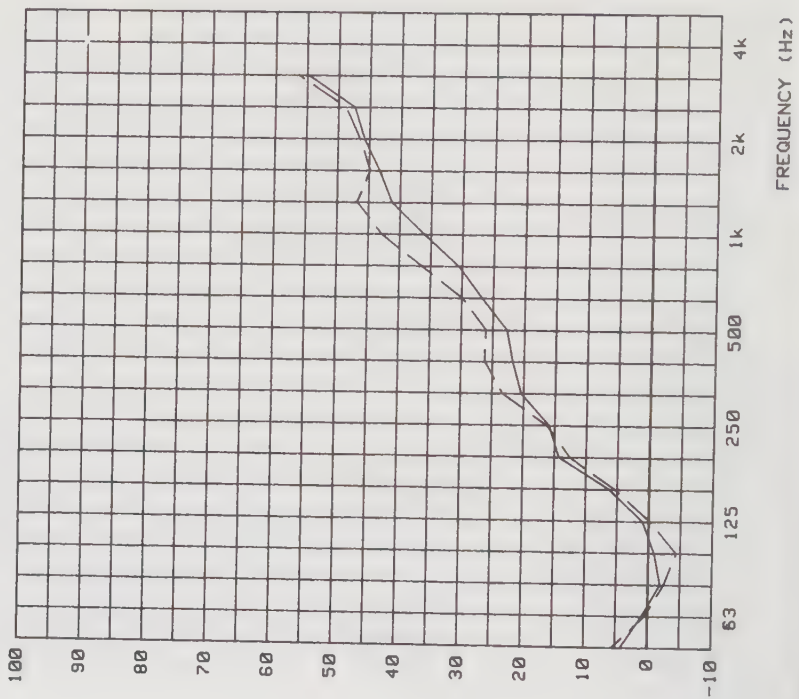
50 mm concrete tiles
50 mm mineral wool
type A
200 mm lightweight
concrete



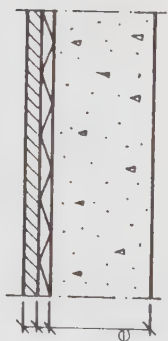
50 mm concrete tiles
50 mm mineral wool
type B
200 mm lightweight
concrete

FREQUENCY (Hz)		63		125		250		500		1k		2k		4k						
(1)	----- dXTR51	4.2	0.9	-1.9	-0.9	1.0	6.3	14.3	15.9	20.4	21.8	22.8	26.8	30.6	36.1	41.3	43.3	45.8	47.4	54.8
(2)	----- dXTd21	5.6	0.5	-2.5	-4.4	-0.2	5.4	12.6	16.3	23.4	26.2	26.1	29.9	36.4	42.9	46.8	44.8	46.5	48.8	56.3

DELTA-L10 (dB)



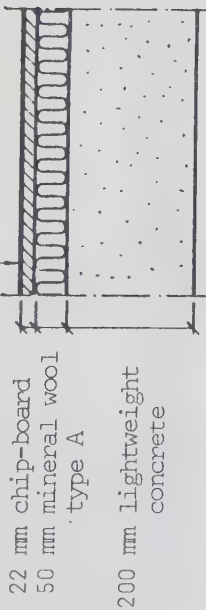
22 mm chip-board
50 mm mineral wool
type A
160 mm dense concrete



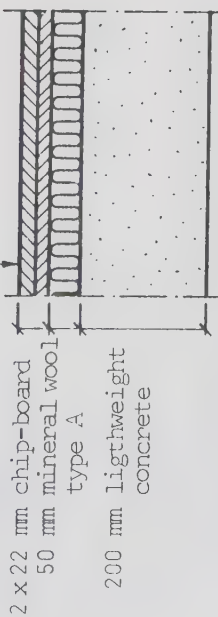
22 mm chip-board
20 mm foam-plastic
type d
160 mm dense concrete

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k										
(1)	delSA1	-0.8	0.3	0.0	-2.5	2.3	3.7	6.3	13.9	15.1	18.8	23.6	27.0	31.0	34.7	38.3	39.4	36.5
(2)	delSA2	-0.2	1.2	-3.0	-2.9	3.3	3.6	7.2	13.8	14.8	17.7	22.1	25.8	30.8	36.3	39.9	40.2	37.3
(3)	delSA4	1.7	2.7	-2.5	-1.9	5.1	5.3	9.0	14.8	15.8	18.0	21.9	25.8	30.6	36.2	41.1	41.7	39.1

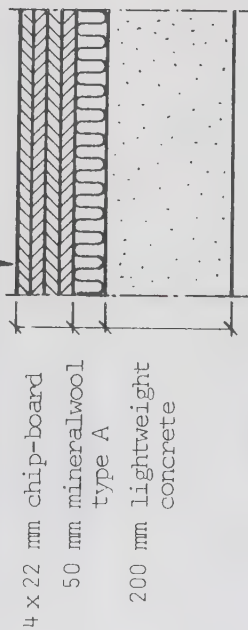
1.9 mm plastic carpet



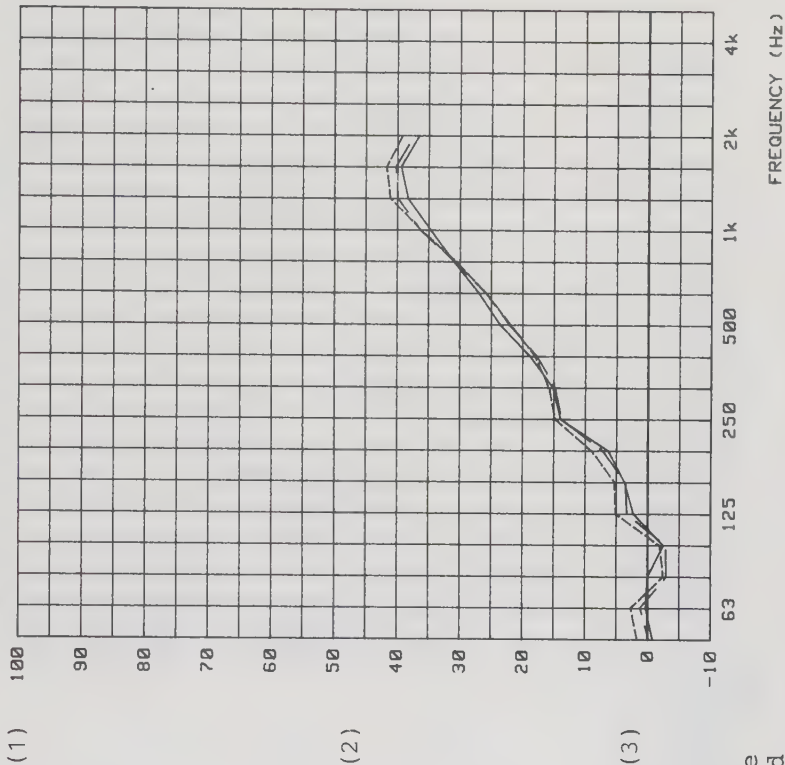
1.9 mm plastic carpet



1.9 mm plastic carpet



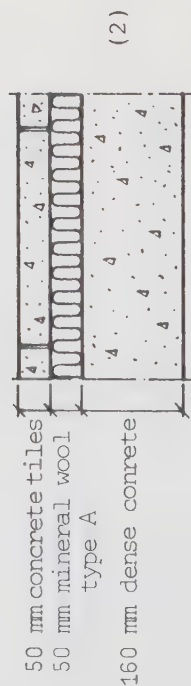
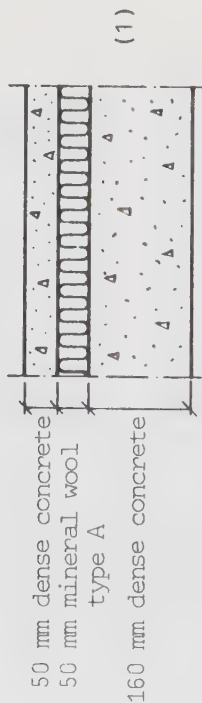
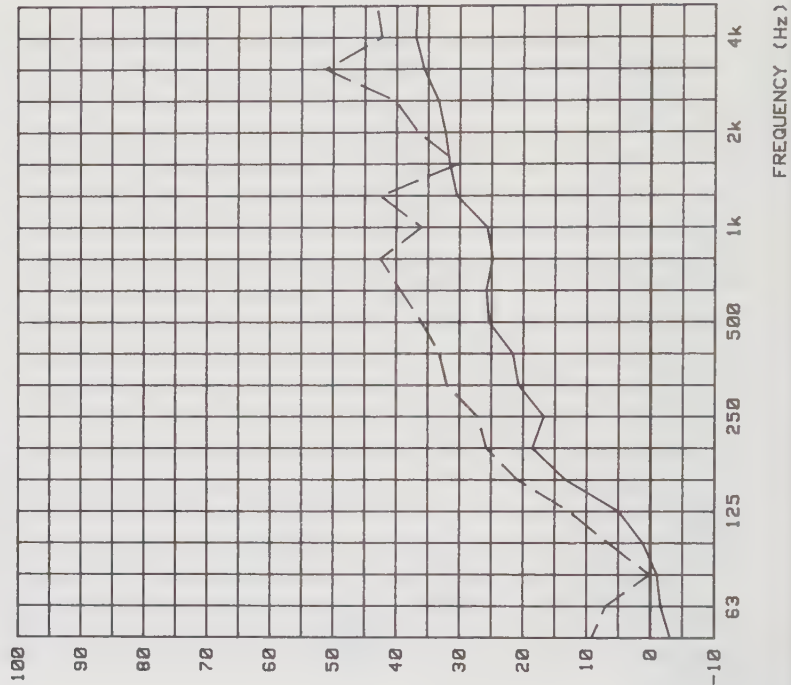
DELTA-L10 (dB)



Note: The reference structural slab has the same plastic carpet as the chip board

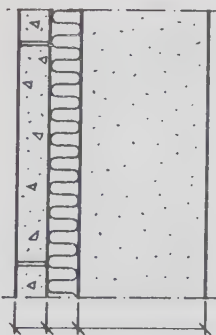
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k														
(1) ——— dXTA5p	-3.1	-1.6	-1.0	1.2	5.0	13.3	18.5	16.7	20.7	21.6	25.4	25.8	24.8	25.7	30.5	31.6	32.3	33.4	35.7	37.0	36.0
(2) ——— dXTA5t	9.2	7.0	0.1	6.6	12.7	20.9	25.8	27.2	32.0	33.2	36.0	39.3	42.5	36.0	42.5	30.2	36.6	40.2	51.2	42.3	43.1

DELTA-L10 (dB)

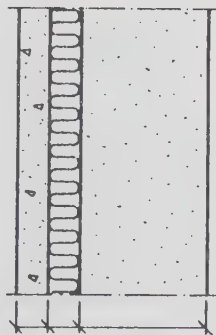


FREQUENCY (Hz)		63	125	250	500	1k	2k	4k				
(1)	delXAt	0.5 10.1	1.4 1.9	12.3 16.0	22.0 26.4	28.1 31.5	34.8 39.3	40.8 30.9	36.0 26.5	29.5 31.0	38.9 23.6	24.2
(2)	delXAp	1.0 4.6	-2.8 -4.0	2.7 -0.2	8.0 12.5	13.6 15.6	18.6 19.0	17.9 17.1	20.2 22.4	22.6 21.0	19.9 17.1	16.6

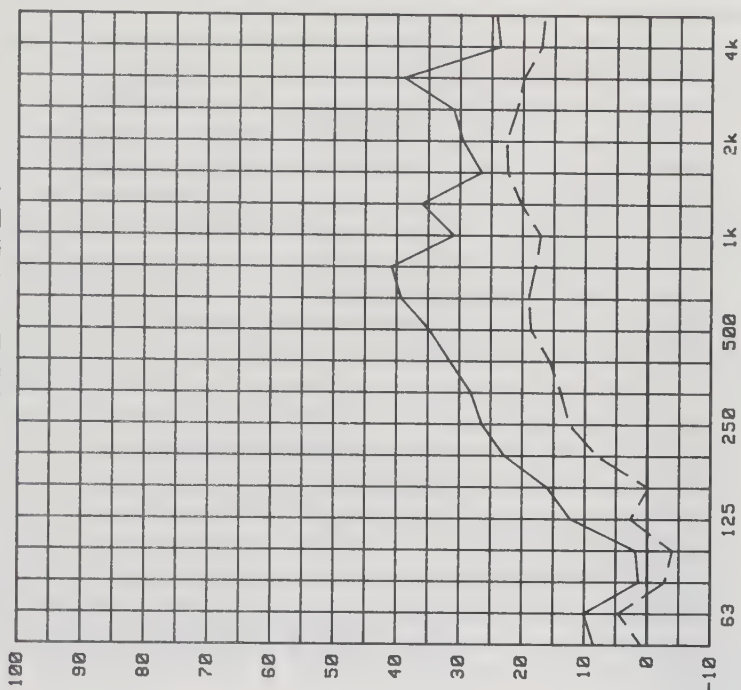
DELTA-L10 (dB)



50 mm concrete tiles
50 mm mineral wool
type A
200 mm lightweight
concrete

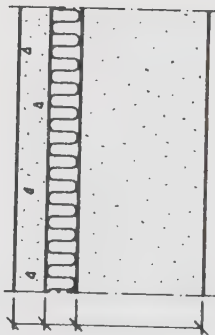


50 mm dense concrete
50 mm mineral wool
type A
200 mm lightweight
concrete

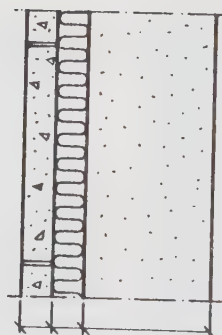


FREQUENCY (Hz)

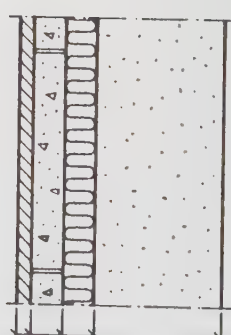
FREQUENCY (Hz)	63	125	250	500	1k	2k	4k														
(1) ----- delXBp	-0.9	-1.7	-0.1	4.1	6.3	1.8	14.2	17.1	17.5	17.4	20.6	20.7	22.8	27.3	27.2	26.8	27.5	27.1	25.8	26.3	
(2) ----- delXBt	4.0	6.7	10.6	12.1	19.5	21.6	27.8	30.9	33.1	36.9	39.3	43.6	44.7	36.0	41.2	29.9	33.0	35.6	44.6	31.9	32.1
(3) ----- delXBu	-0.3	3.8	7.1	10.8	17.2	18.5	24.2	27.5	29.9	31.5	33.9	40.0	42.4	48.9	48.3	59.2	54.8	58.9			



50 mm dense concrete
50 mm mineral wool
type B
200 mm lightweight
concrete

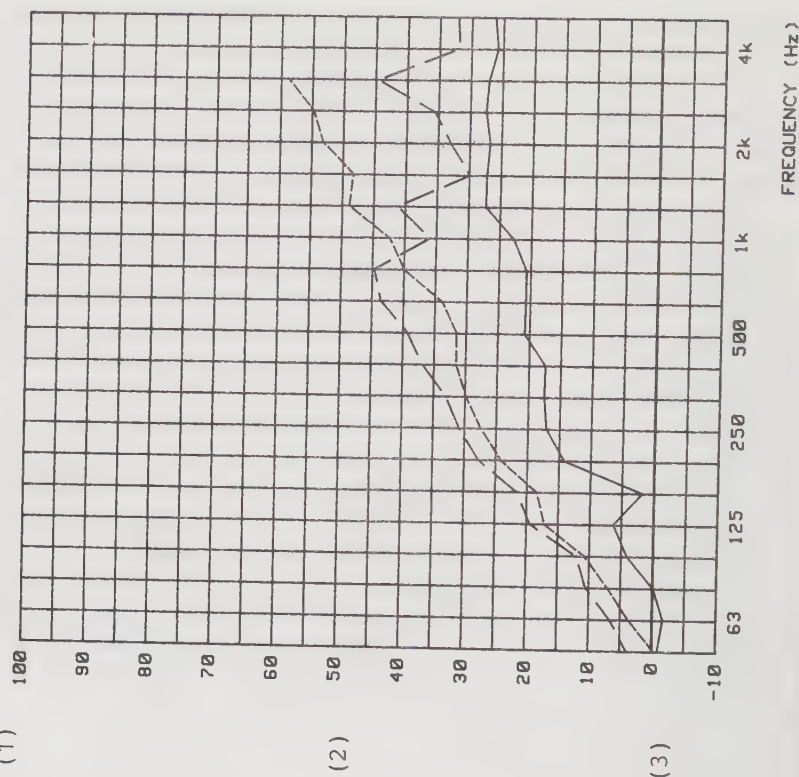


50 mm concrete tiles
50 mm mineral wool
type B
200 mm lightweight
concrete



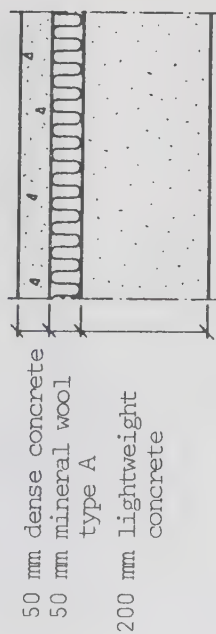
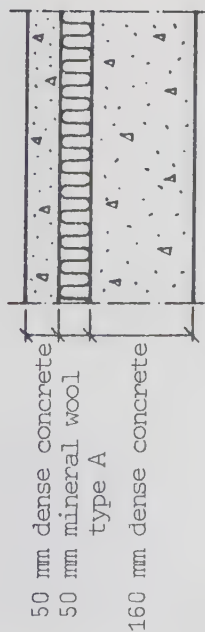
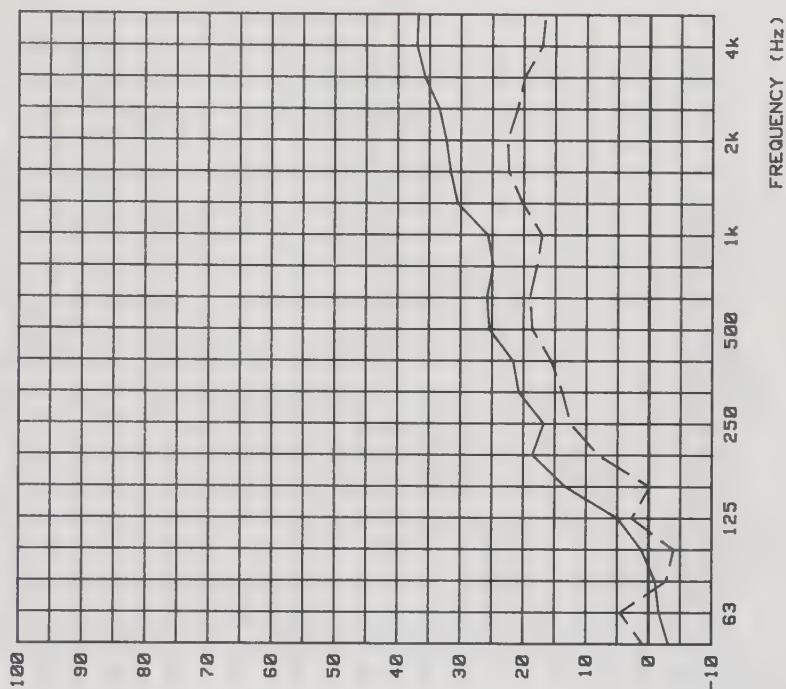
22 mm chip-board
50 mm concrete tiles
50 mm mineral wool
type B
200 mm lightweight
concrete

DELTA-L10 (dB)



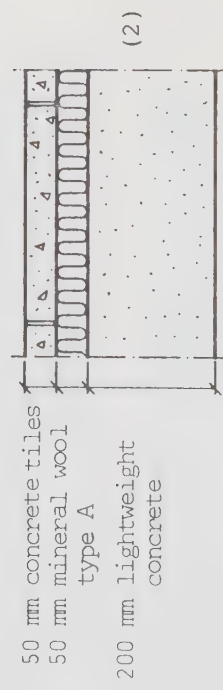
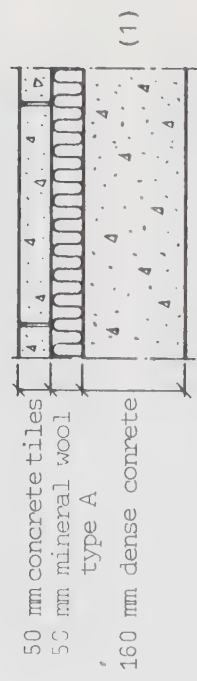
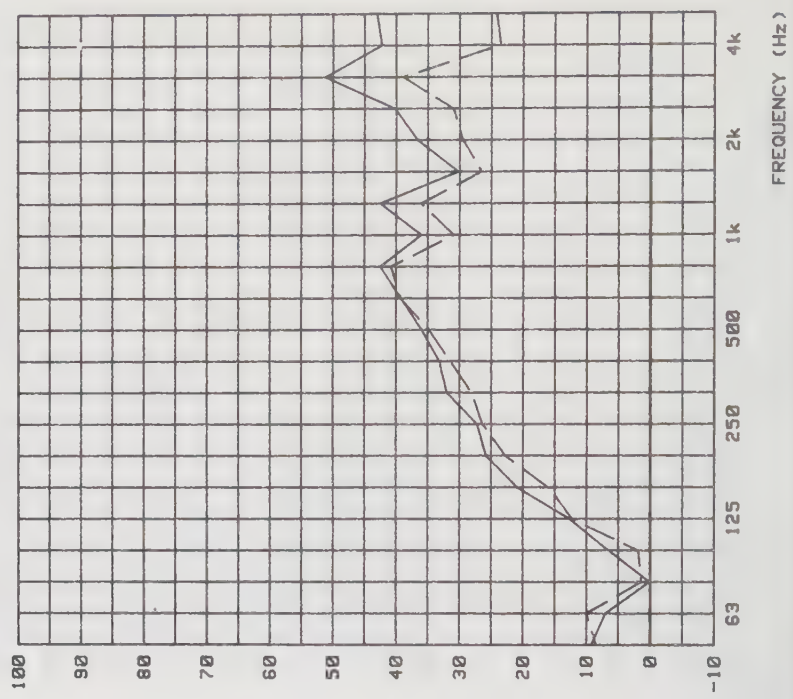
[illegible]

DELTA-L10 (dB)



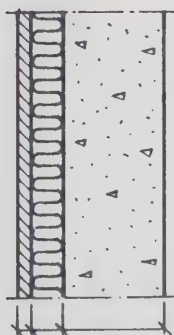
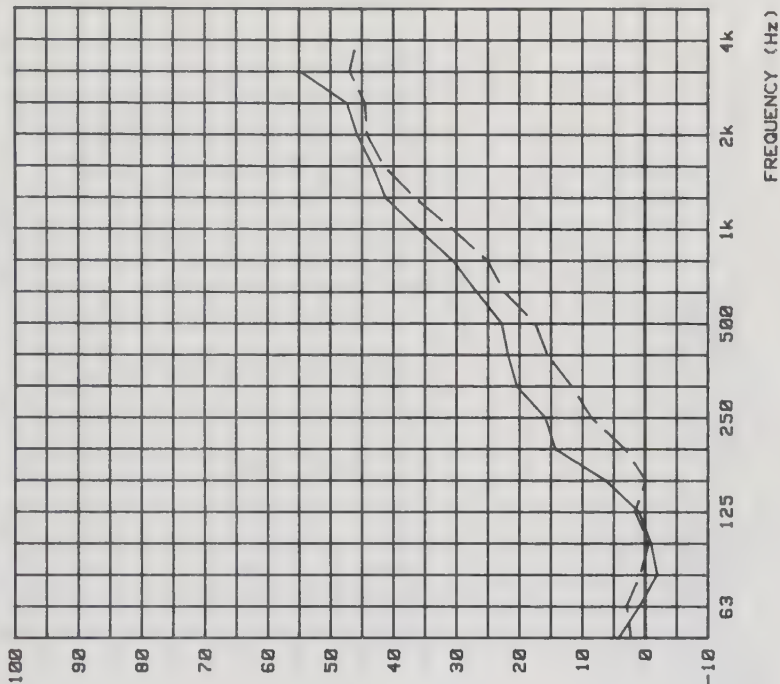
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	— dXtRst	9.2	7.0	0.1	6.6	12.7	20.9	25.8	27.2	32.0	33.2	36.0	39.3	42.5	36.0	42.5	30.2	36.6	40.2	51.2	42.3	43.1
(2)	— — — delXAt	8.5	10.1	1.4	1.9	12.3	16.0	22.0	26.4	28.1	31.5	34.8	39.3	40.8	30.9	36.0	26.5	25.5	31.0	38.9	23.6	24.2

DELTA-L10 (dB)

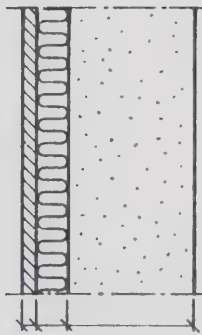


FREQUENCY (Hz)		63	125	250	500	1k	2k	4k
(1)	----- dXTA51	4.2	0.9 -1.9 -0.9	1.0	6.3 14.3 15.9 20.4 21.8	22.8 26.8 30.6 36.1	41.3 43.3 45.8	47.4 54.8
(2)	----- delXR1	2.3	3.0 0.8 -0.6	1.6 -0.1 3.2	8.6 11.7 15.6	17.5 22.4 25.1 30.6 36.6	41.5 44.2 44.5 46.9	45.8

DELTA-L10 (dB)



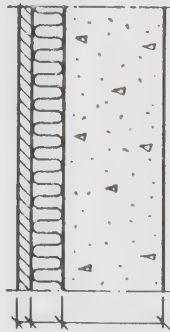
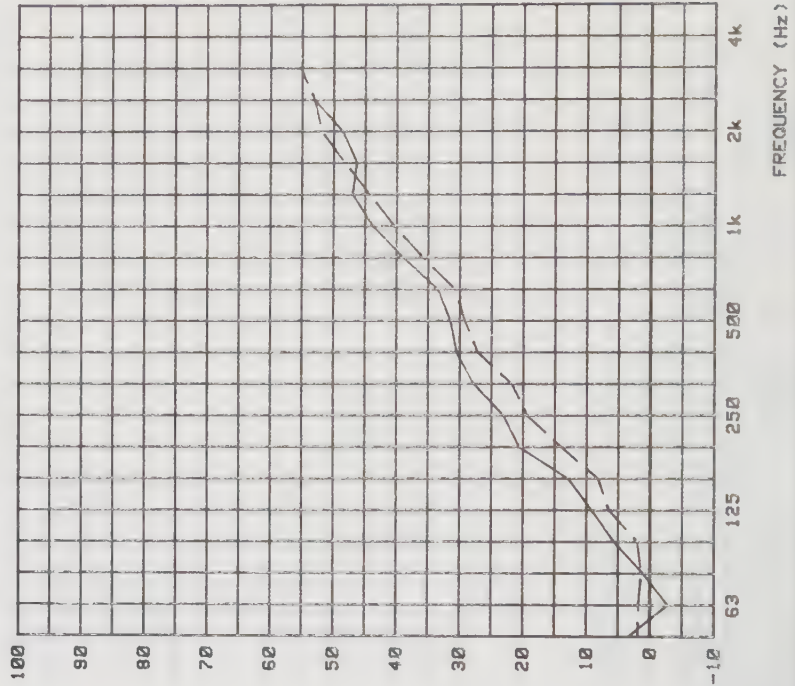
22 mm chip-board
50 mm mineral wool
type A
160 mm dense concrete



22 mm chip-board
50 mm mineral wool
type A
200 mm lightweight
concrete

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k			
(1) ——— dXTC51	3.4 -2.8	1.1 5.7	9.2 12.9	20.6 23.1	27.8 30.5	31.5 33.5	39.0 43.8	47.0 48.6	53.1	
(2) ——— d1XC1	1.9 1.8	1.4 2.0	6.5 8.2	14.0 19.4	21.8 27.2	28.2 30.7	35.0 40.4	44.3 46.2	51.9 53.0	55.2

DELTA-L10 (dB)



22 mm chip-board
50 mm glass fibre
type C
160 mm dense concrete



22 mm chip-board
50 mm glass fibre
(type C)
200 mm lightweight
concrete

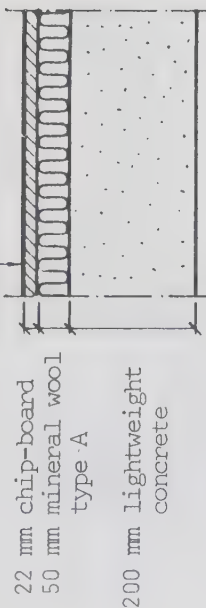
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k													
(1)	— delX50	3.3	1.7	2.4	1.9	3.3	2.6	3.0	4.4	5.3	7.3	8.6	12.2	15.4	18.3	22.7	29.2	31.8	30.6	27.8	0.0
(2)	— delX51	0.2	0.5	0.9	0.5	2.6	5.7	6.4	7.9	6.4	7.6	11.4	11.9	14.7	16.3	17.1	16.2	15.0			
(3)	— delXSp	4.4	1.2	2.0	0.6	1.0	-0.3	1.4	1.5	2.1	2.3	3.5	5.3	7.4	10.4	14.1	18.8	23.0	29.5	36.3	

Note: Impact insulation improvement for the plastic carpet itself

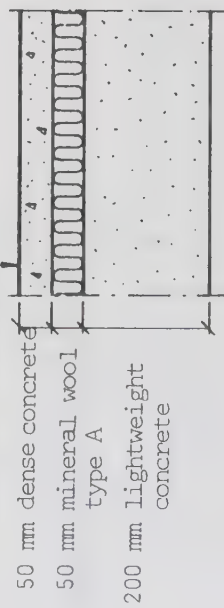
1.9 mm plastic carpet



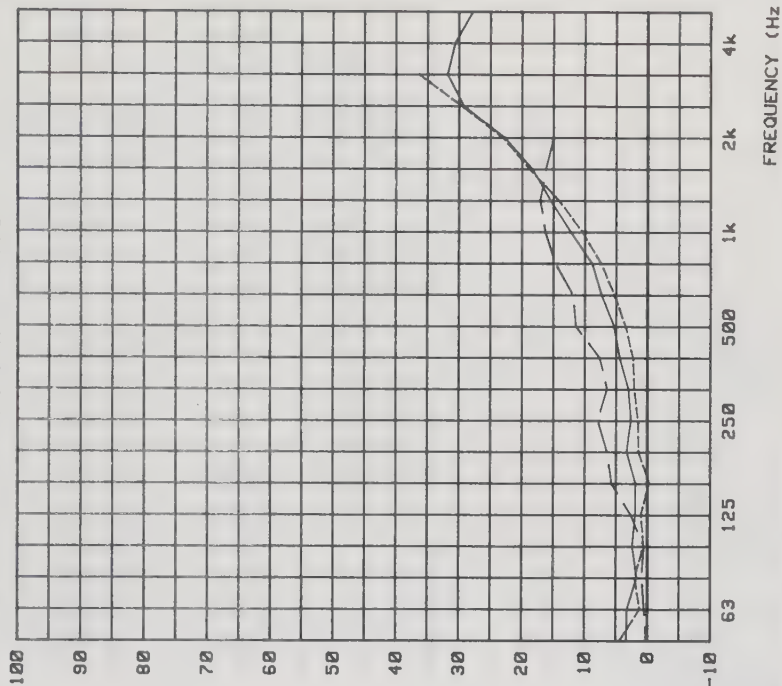
1.9 mm plastic carpet



1.9 mm plastic carpet



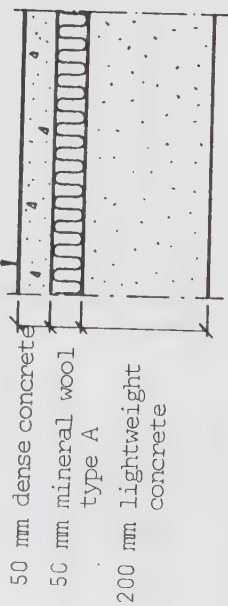
DELTA-L10 (dB)



[illegible]

Note: Impact insulation improvement for the plastic carpet itself

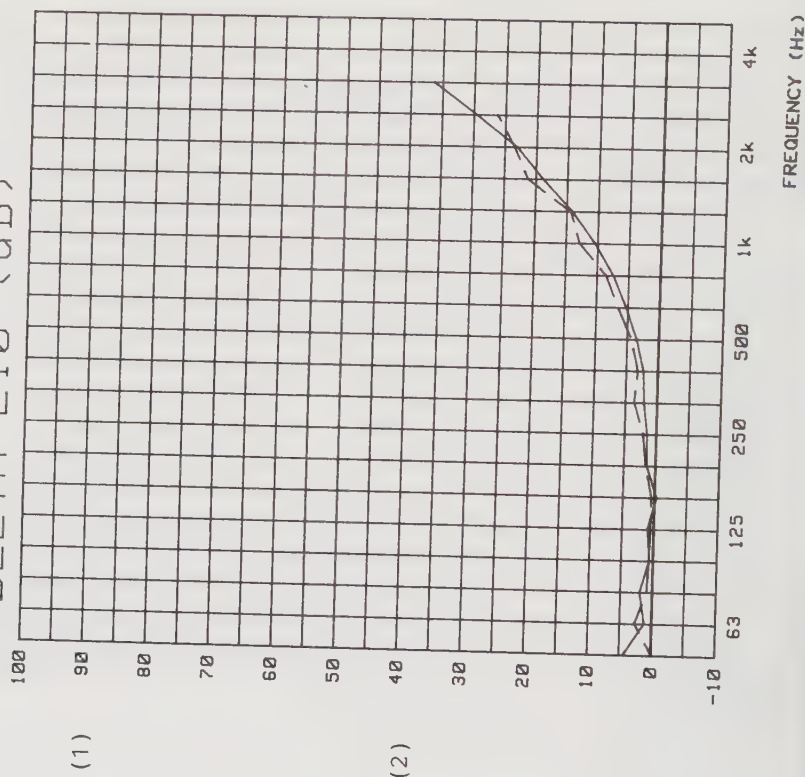
1.9 mm plastic carpet



1.9 mm plastic carpet

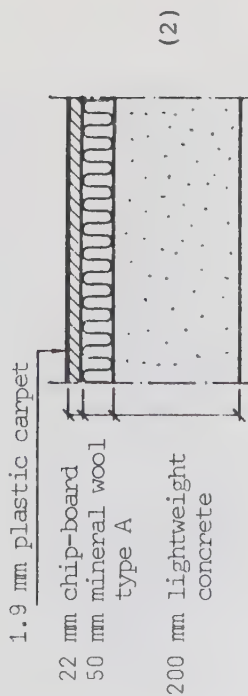
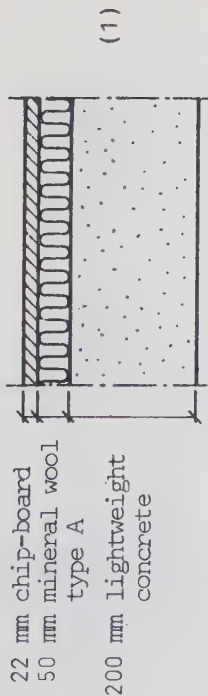
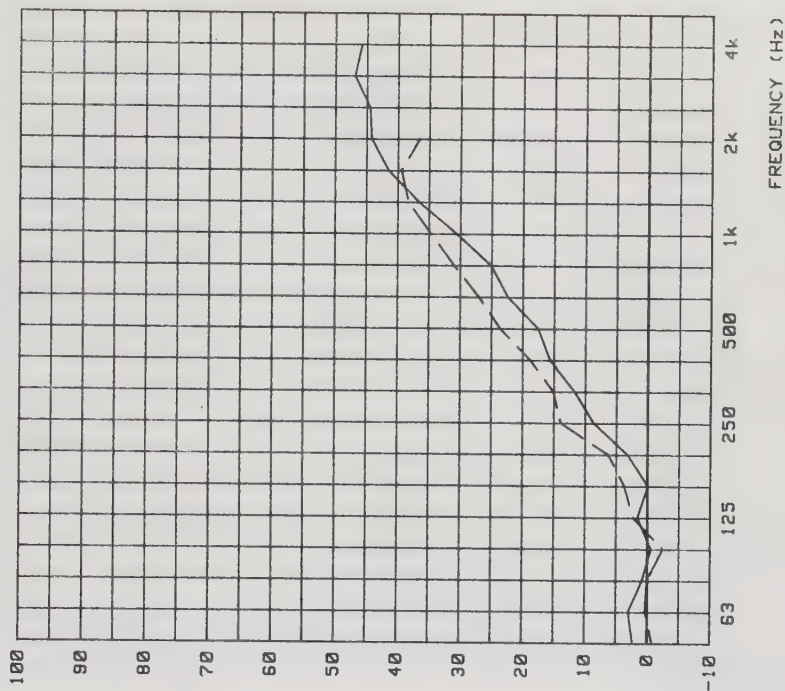


DELTA-L10 (dB)



FREQUENCY (Hz)		63	125	250	500	1k	2k	4k													
(1)	— del1A1	2.3	3.0	0.8	-0.6	1.6	-0.1	3.2	0.6	11.7	15.6	17.5	22.4	25.1	30.6	36.6	41.5	44.2	44.5	46.9	45.8
(2)	— — del1A1	-0.8	0.3	0.0	-2.5	2.3	3.7	6.3	13.9	15.1	18.8	23.6	27.0	31.0	34.7	39.3	39.4	36.5			

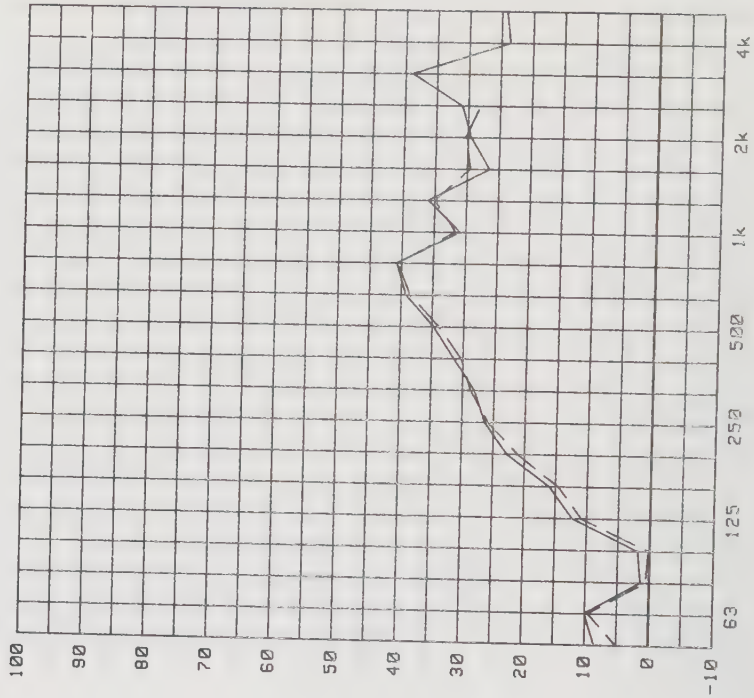
DELTA-L10 (dB)



Note: The reference structural slab for (1) has no plastic carpet
The reference structural slab for (2) has the same plastic carpet as the chip board

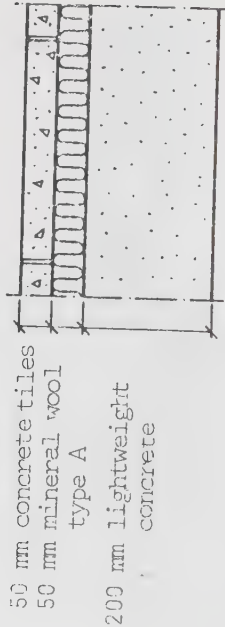
FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	delXAt	8.5	10.1	1.4	1.9	12.3	16.0	22.8	26.4	28.1	31.5	34.8	39.3	40.8	30.9	36.0	26.5	29.5	31.0	38.9	23.6	24.2
(2)	delSAt	5.1	9.6	0.6	0.1	10.9	14.6	21.1	25.9	28.7	30.3	33.9	38.6	40.6	31.6	35.0	29.5	30.3	28.1			

DELTA-L10 (dB)

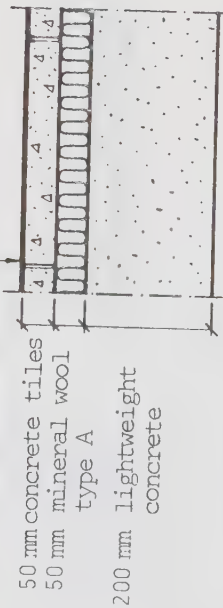


(1)

(2)



1.9 mm plastic carpet



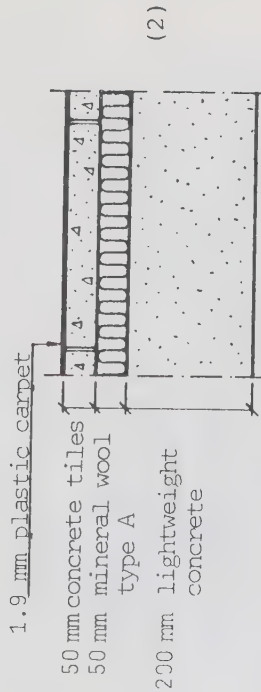
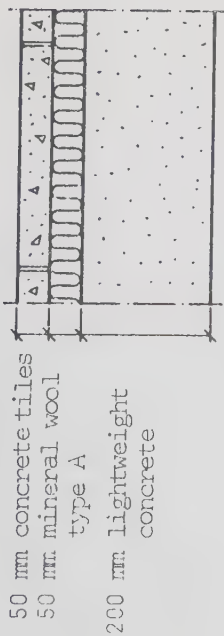
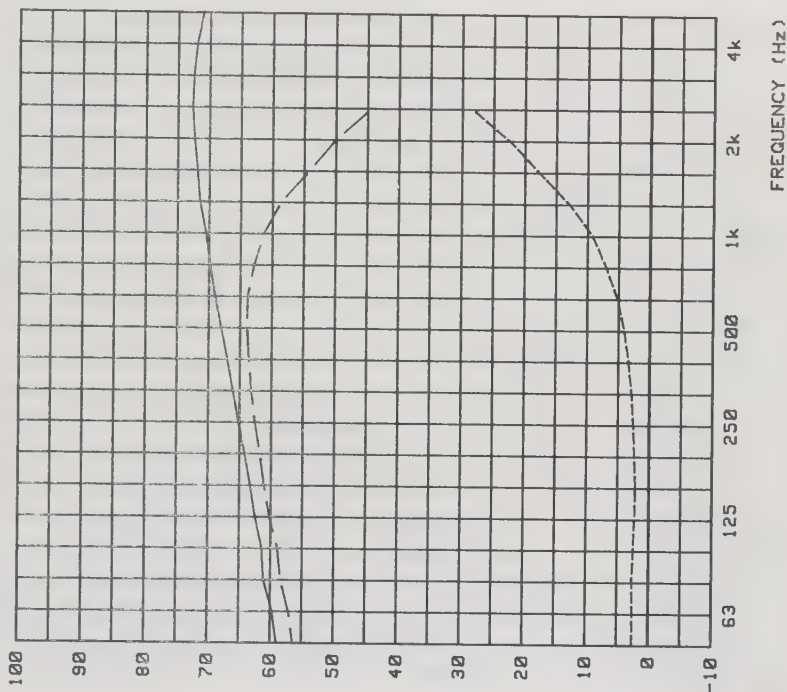
Note: The reference slab for (1) has no plastic carpet

The reference slab for (2) has the same plastic carpet as the tiles

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k	I1																
(1)	----- Fx _t _{mv}	59.0	59.7	61.1	61.4	62.5	63.3	64.2	65.2	66.1	67.0	68.0	69.0	70.5	71.5	71.9	72.4	72.7	72.5	72.0	71.0	69.6	66.9	999	
(2)	----- Fst _{mv}	56.4	57.1	58.4	58.9	60.2	61.1	61.8	62.6	63.2	63.6	63.9	63.8	62.8	61.3	58.6	54.2	50.0	44.4						999
(3)	----- dFXSrt	2.6	2.6	2.7	2.5	2.3	2.2	2.4	2.6	2.9	3.4	4.1	5.2	7.0	9.2	12.9	17.7	22.4	28.3						

FORCE LEVEL

dB (relative cal)

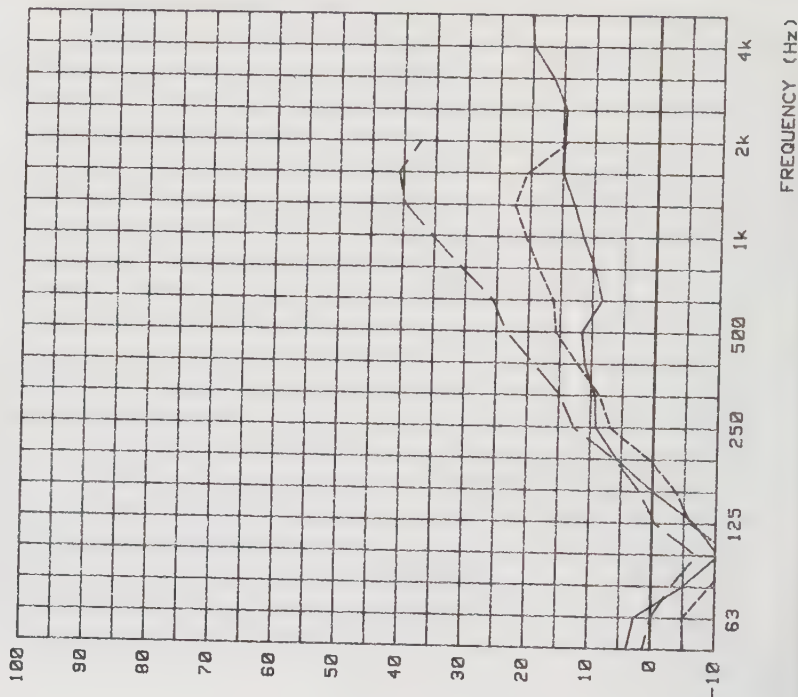


Difference (3)

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	——— dRLa03	3.7	2.6	-4.9	10.5	-6.7	0.0	5.7	9.1	9.3	11.0	11.5	8.4	9.6	11.5	13.0	14.9	14.7	14.5	16.6	19.8	20.2
(2)	——— dLa03	1.2	0.0	-3.1	-7.1	-0.4	2.1	6.0	12.6	14.8	19.1	23.5	25.6	30.6	35.3	39.7	40.7	37.3				
(3)	----- dLa03kF	-5.1	-4.9	-9.4	13.0	-6.1	-3.7	0.5	6.8	8.6	12.2	15.7	16.1	10.5	20.6	22.4	20.6	14.3				

- (1) ΔR Transmission loss improvement
- (2) ΔL Impact level improvement
- (3) $\Delta L + \Delta F$ Impact level improvement corrected with the difference in force spectra between the floating floor and the structural slab

DELTA-R/L10 (dB)

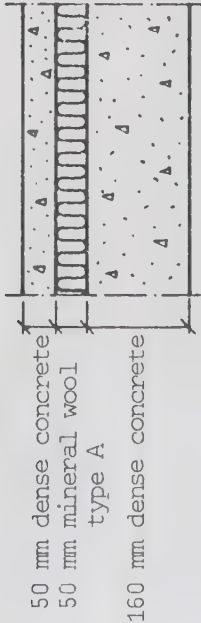
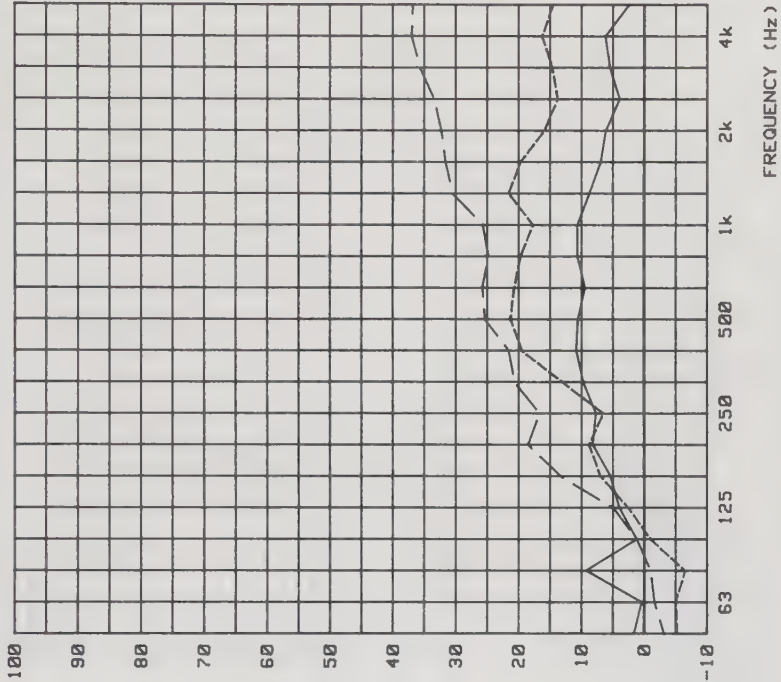


Note: The reference slab has the same plastic carpet as the chip board

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k														
(1) ——— dRTAsp	1.6	0.4	9.3	1.2	4.0	5.4	0.2	7.7	9.7	10.8	10.5	9.4	10.6	10.6	8.7	6.9	6.1	3.9	5.5	6.2	2.4
(2) ——— dXTAsp	-3.1	-1.6	-1.0	1.2	5.0	13.3	18.5	16.7	20.7	21.6	25.4	25.8	24.8	25.7	30.5	31.6	32.3	33.4	35.7	37.0	36.8
(3) - - - - - dLaSsp	-5.2	-5.0	-6.5	-1.0	2.6	7.0	0.8	6.5	13.0	19.5	21.3	20.7	19.7	17.7	21.6	19.7	15.9	13.8	14.7	16.3	14.6

- (1) ΔR Transmission loss improvement
- (2) ΔL Impact sound insulation improvement
- (3) ΔL_a Acceleration level improvement

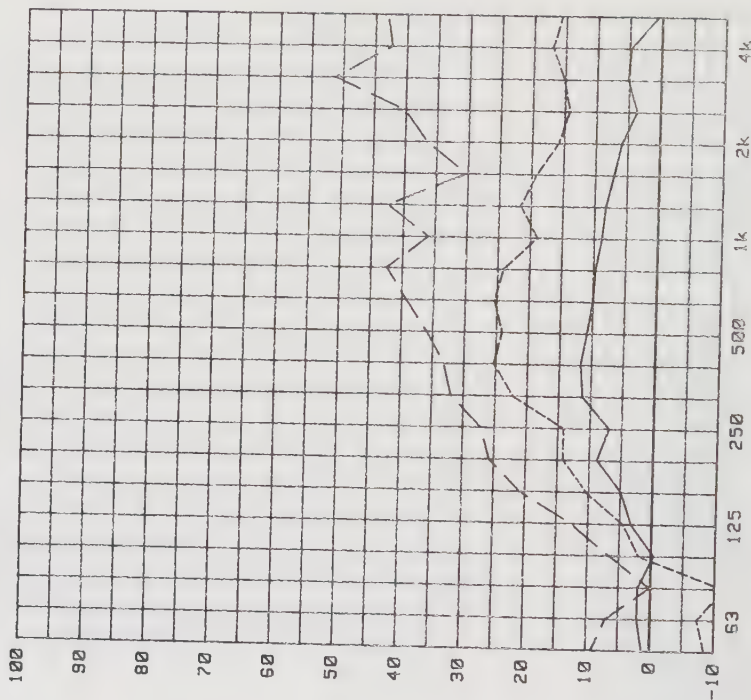
DELTA-R/L10/La (dB)



FREQUENCY (Hz)	63	125	250	500	1k	2k	4k
(1) ΔR Transmission loss improvement							
(2) ΔL Impact sound insulation improvement							
(3) ΔL_a Acceleration level improvement							

- (1) ΔR Transmission loss improvement
- (2) ΔL Impact sound insulation improvement
- (3) ΔL_a Acceleration level improvement

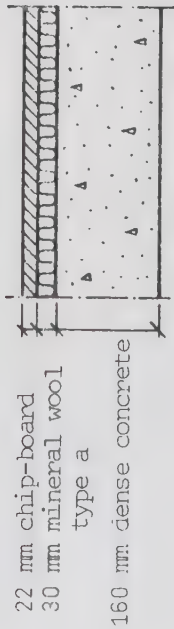
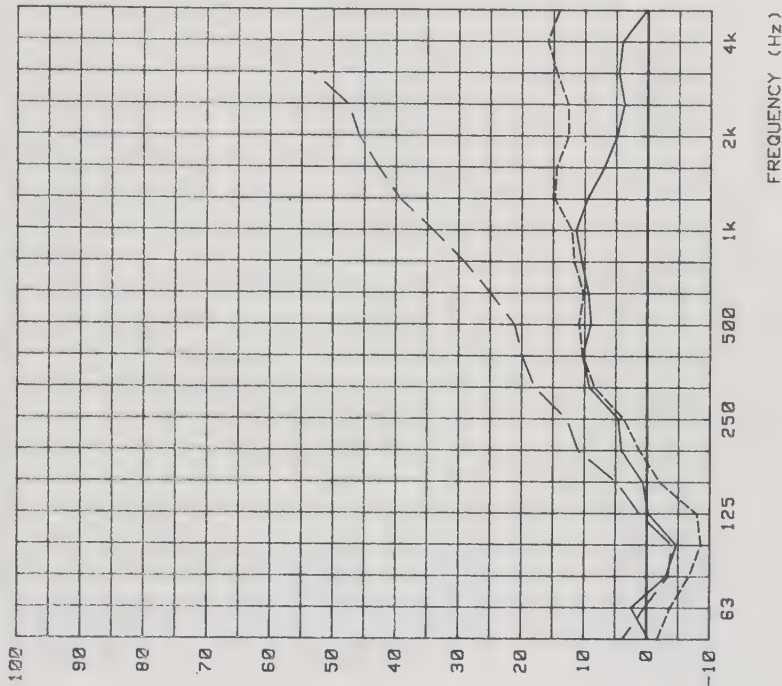
DELTA-R/L10/La (dB)



FREQUENCY (Hz)	63	125	250	500	1k	2k	4k														
(1) ——— dR _{ta03}	-0.3	2.5	-3.1	-4.7	-0.2	0.7	4.1	4.6	9.2	10.3	9.0	9.4	10.5	11.4	9.6	6.9	4.9	3.7	4.6	4.0	0.2
(2) ——— dX _{ta03}	3.8	0.3	-3.4	-4.0	1.3	4.9	11.0	12.8	17.9	19.9	21.1	25.0	29.2	34.1	39.4	42.8	45.9	47.6	53.2		
(3) ----- dL _{aTa3}	-1.7	-3.7	-6.7	-8.6	-7.9	-2.0	1.2	3.7	8.4	10.3	10.9	10.2	11.7	12.1	14.8	14.5	12.6	12.7	14.5	15.0	14.1

- (1) ΔR Transmission loss improvement
- (2) ΔL Impact sound insulation improvement
- (3) ΔL_a Acceleration level improvement

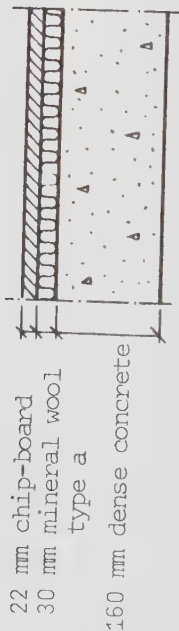
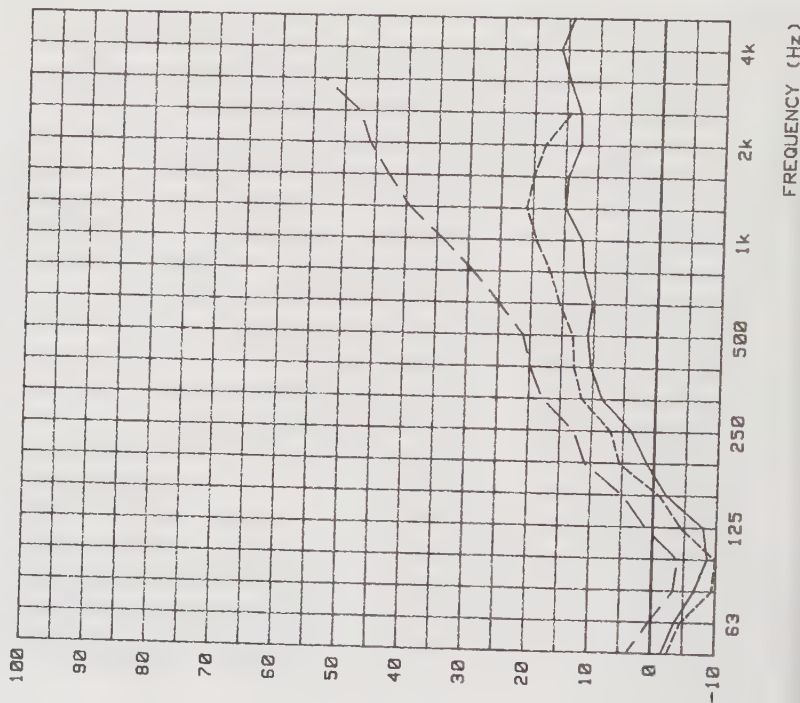
DELTA-R/L10/La (dB)



FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	----- dLaTa3	-1.7	-3.7	-6.7	-8.6	-7.9	-2.0	1.2	3.7	8.4	10.3	10.9	10.2	11.7	12.1	14.0	14.5	12.6	12.7	14.5	16.0	14.1
(2)	----- dXTa03	3.8	0.3	-3.4	-4.0	1.3	4.9	11.0	12.8	17.9	19.9	21.1	25.0	29.2	34.1	39.4	42.8	45.9	47.6	53.2		
(3)	----- dXTaKF	-2.5	-4.6	-9.4	-9.9	-4.4	-0.9	5.5	7.0	11.7	13.0	13.3	15.5	17.1	19.4	21.0	20.0	18.3	14.4			

- (1) ΔL_a Acceleration level improvement
- (2) ΔL Impact level improvement
- (3) $\Delta L + \Delta F$ Impact level improvement corrected with the difference in force spectra between the floating floor and the structural slab.

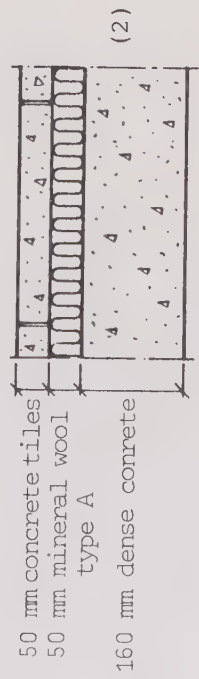
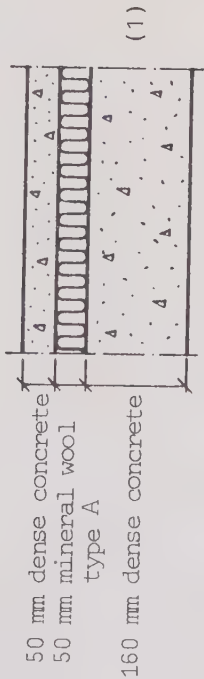
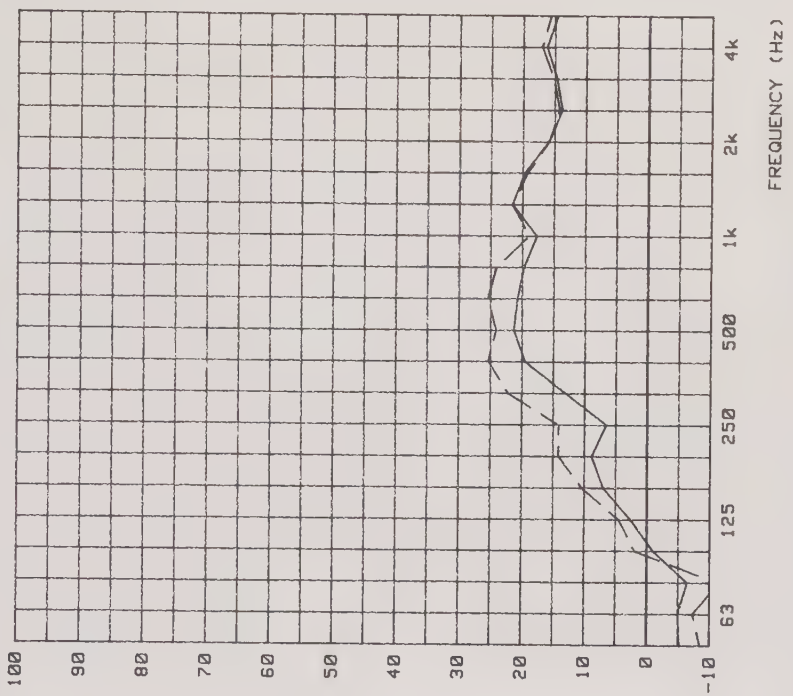
DELTA-L10/La (dB)



FREQUENCY (Hz)		63		125		250		500		1k		2k		4k								
(1)	— dLaRSp	-5.2	-5.0	-6.5	-1.0	2.6	7.0	8.0	6.5	13.0	19.5	21.3	20.7	19.7	21.6	19.7	15.9	13.8	14.7	16.3	14.6	
(2)	— dLaRst	-8.5	-7.3	11.5	2.0	4.5	10.4	14.1	14.1	22.2	25.3	24.1	25.4	24.1	18.9	21.6	19.2	15.8	14.2	15.2	17.0	15.6
												</										

Acceleration level improvement

DELTA-La (dB)



APPENDIX IV

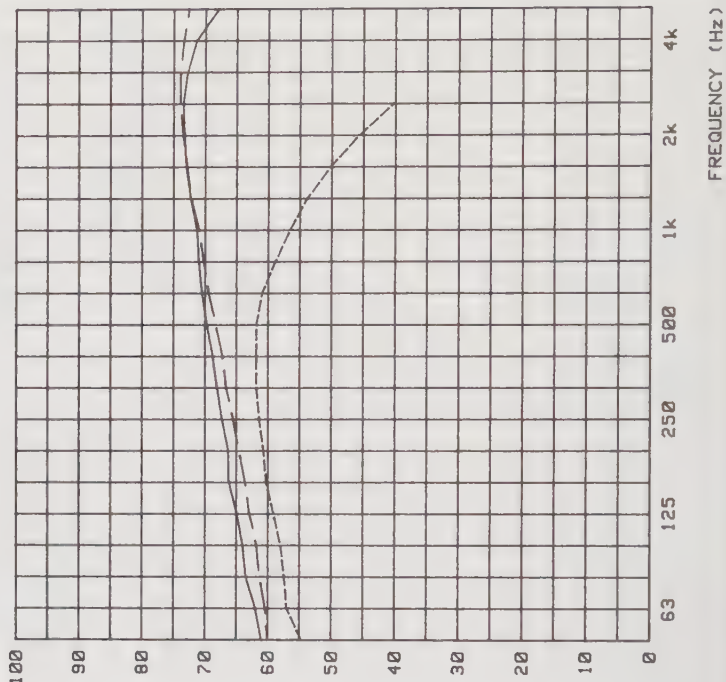
APPENDIX IV

- IV.1 Force levels for the standard ISO-hammer on different slabs compared to the 160 mm concrete slab:
- .1 Lightweight concrete
 - .2 50 mm concrete slab on mineral wool
22 mm chip board on mineral wool
 - .3 Difference curve between the chip board and the 160 mm structural slab
 - .4 50 mm concrete tiles on mineral wool
22 mm chip board + 50 mm concrete tiles on mineral wool
 - .5 Measurement results for different striking positions on a concrete tile

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k														
(1)	— FTo_mv	61.1	62.0	63.5	64.0	64.9	66.2	66.3	67.3	68.1	68.8	69.7	70.5	71.0	71.3	72.3	73.0	73.3	73.5	72.8	71.4	67.9
(2)	— — FXp_mv	60.1	60.5	61.4	61.9	63.0	63.7	64.7	65.6	66.7	67.7	68.4	69.5	70.3	71.0	72.3	72.9	73.5	74.0	73.9	73.4	72.6
(3)	----- Fal_mv	54.8	57.1	59.5	61.1	62.2	63.4	64.8	66.1	67.5	68.9	70.1	71.1	71.9	72.6	73.9	75.0	75.7	76.2	74.5	72.3	68.3

FORCE LEVEL

dB (relative cal)



(1)

(2)

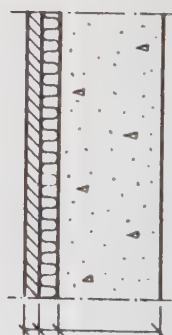
(3)



160 mm dense concrete



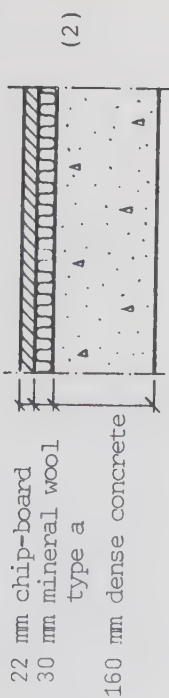
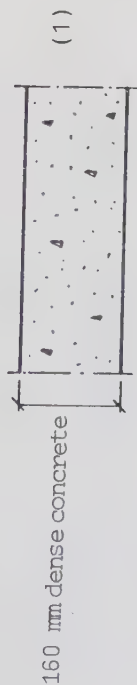
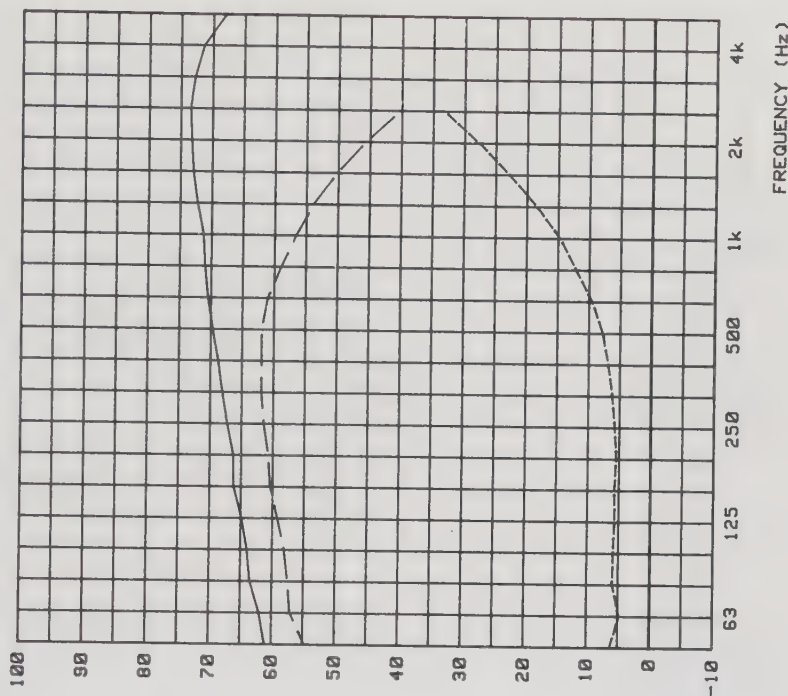
50 mm dense concrete
50 mm mineral wool
type A
160 mm dense concrete



22 mm chip-board
30 mm mineral wool
type a
160 mm dense concrete

FORCE LEVEL

dB (relative cal)

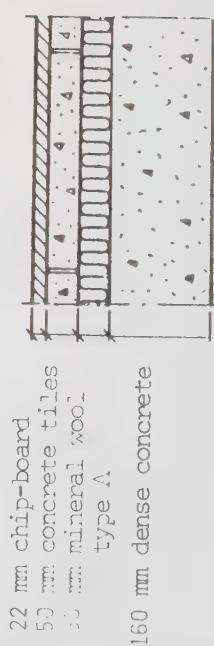
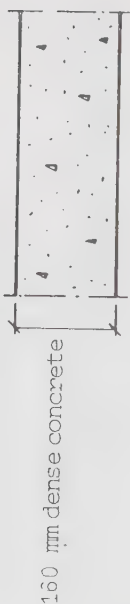
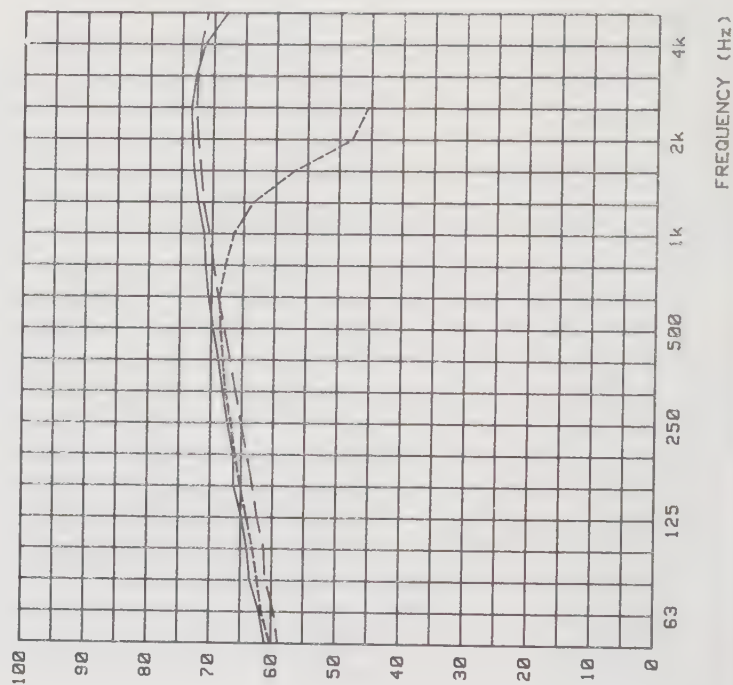


Difference (3)

FREQUENCY (Hz)	63	125	250	500	1k	2k	4k
(1) ——— FTo_mv	61.1	63.5	64.0	64.9	66.2	66.3	67.3
(2) ——— FXt_mv	59.0	61.1	61.4	62.5	63.3	64.2	65.2
(3) ——— FXu_mv	60.3	61.7	62.4	63.2	64.2	65.3	66.1

FORCE LEVEL

dB (relative cal)



IV.1.5

TEKNISKA HÖGSKOLAN ILUND Byggnadsakustik	KRÄFT; ACC PA ISO-HAMMARE FTA05t	KURVBLAD NR
		MÄTDAG
		SIGN

Frekv.	kant1	horn1	mitt1
16			
20			
25			
32			
40			
50	59.9	57.8	60.3
63	59.3	56.1	60.9
80	61.5	58.3	62.7
100	61.9	60.1	63.0
125	62.6	61.4	63.9
160	64.0	62.0	64.9
200	64.7	62.5	65.8
250	65.6	63.9	66.9
315	66.9	65.0	67.6
400	67.6	65.7	68.0
500	68.6	66.6	69.6

Frekv.	kant1	horn1	mitt1
630	69.6	67.4	70.5
800	70.5	67.9	71.4
1000	71.3	68.1	72.1
1250	72.5	68.6	73.0
1600	73.0	68.4	73.5
2000	73.7	67.4	74.0
2500	74.3	65.6	74.2
3150	74.3	62.0	73.3
4000	74.4	57.0	73.0
5000	73.5	50.0	71.0
6300	70.9		68.6
8000	68.1		65.3
10000			
12500			
16000			
20000			

T Ov mitt1 ——— (Middle)

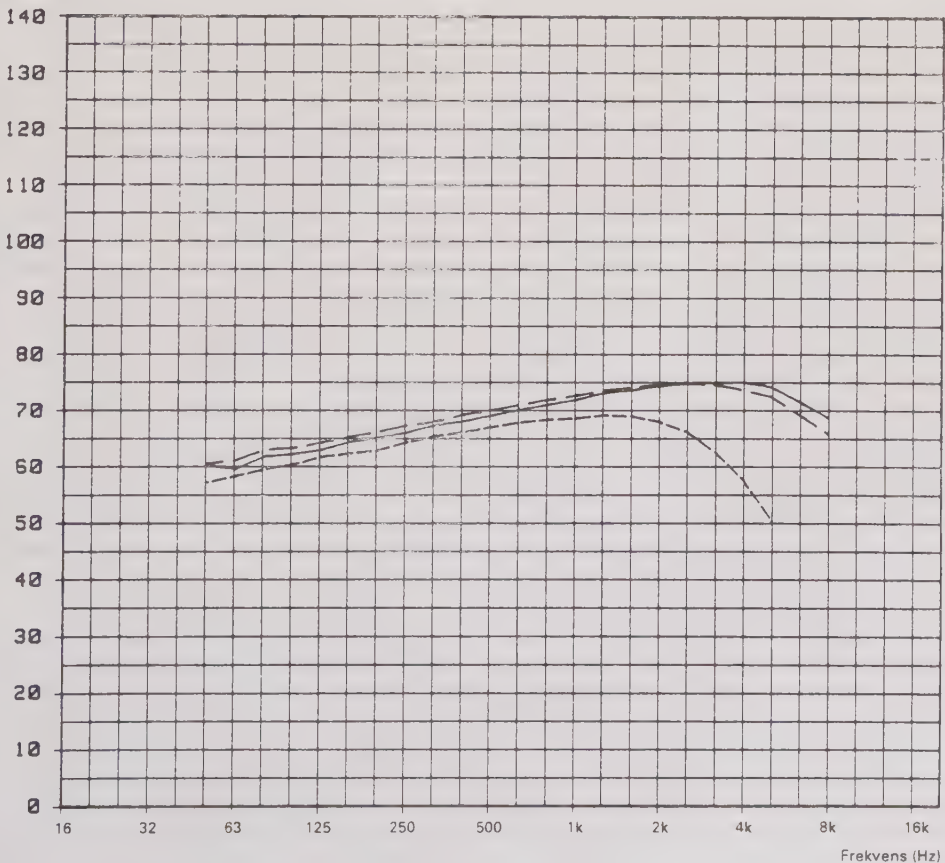
T Ov horn1 ——— (Corner)

T Ov kant1 ——— (Edge)

FORCE LEVEL

(Different striking positions
on a concrete tile)

(dB)



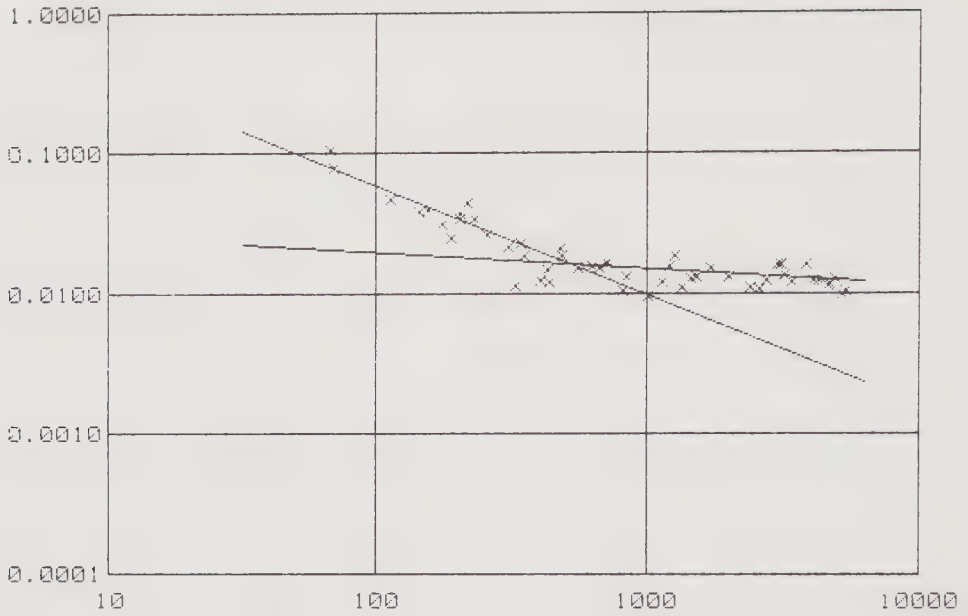
APPENDIX V

APPENDIX V

V.1 Loss factors for the structural slabs and the floating floors

- .1 Structural slab, lightweight concrete
- .2 Structural slab, dense concrete

- .3 Floating floor, chip board on mineral wool a
- .4 Floating floor, concrete slab on mineral wool A
- .5 Floating floor, concrete slab on mineral wool B
- .6 Floating floor, concrete slab on mineral wool C
- .7 Floating floor, concrete slab free
- .8 Floating floor, concrete slab .4-.7 in one figure
- .9 Floating floor, concrete tiles on mineral wool B

Loss factor η 

Frequency (Hz)

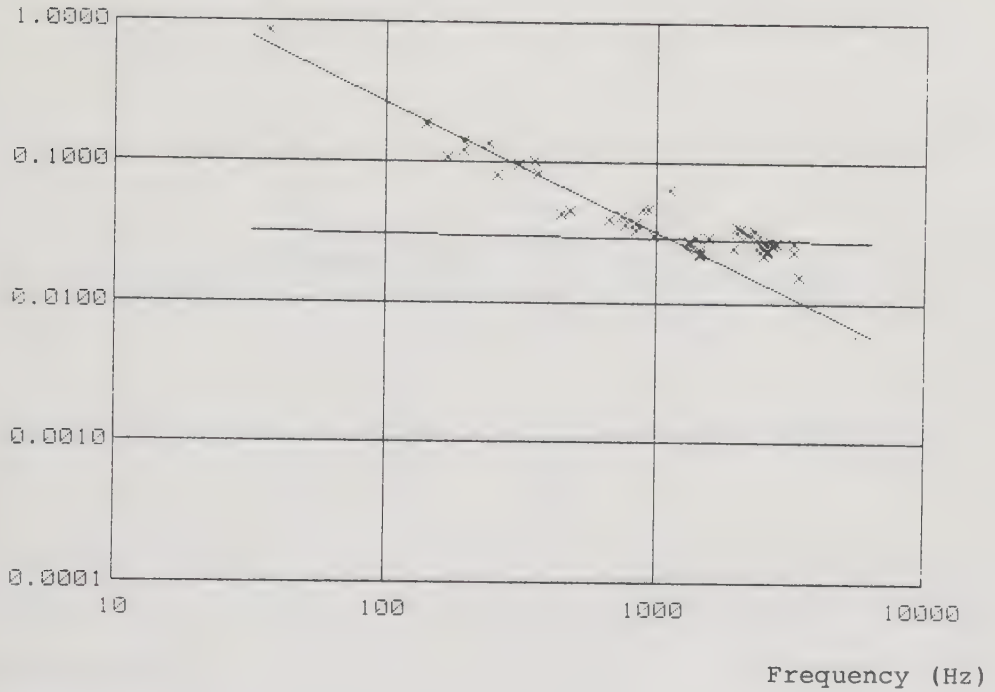
Freq.	η_3
50	0.099
63	0.083
80	0.069
100	0.058
125	0.049
160	0.040
200	0.034
250	0.029
315	0.024
400	0.020
500	0.017
630	0.014
800	0.014
1000	0.014
1250	0.014
1600	0.014
2000	0.014
2500	0.014
3150	0.014
4000	0.014
5000	0.014

160 mm dense concrete

 $3 \times 4 \text{ m}^2$ simply supported

$$\eta_3 \approx 0.058 (100/f)^{0.77} \quad f < 630 \text{ Hz}$$

$$\eta_3 \approx 0.014 \quad f \geq 630 \text{ Hz}$$

Loss factor η 

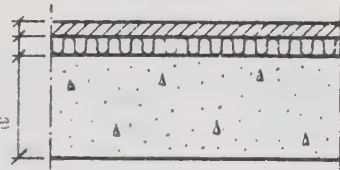
Freq.	η_1
50	0.50
63	0.40
80	0.32
100	0.27
125	0.21
160	0.17
200	0.14
250	0.12
315	0.093
400	0.075
500	0.062
630	0.050
800	0.040
1000	0.033
1250	0.030
1600	0.030
2000	0.030
2500	0.030
3150	0.030
4000	0.030
5000	0.030

22 mm chip-board

30 mm mineral wool

type a

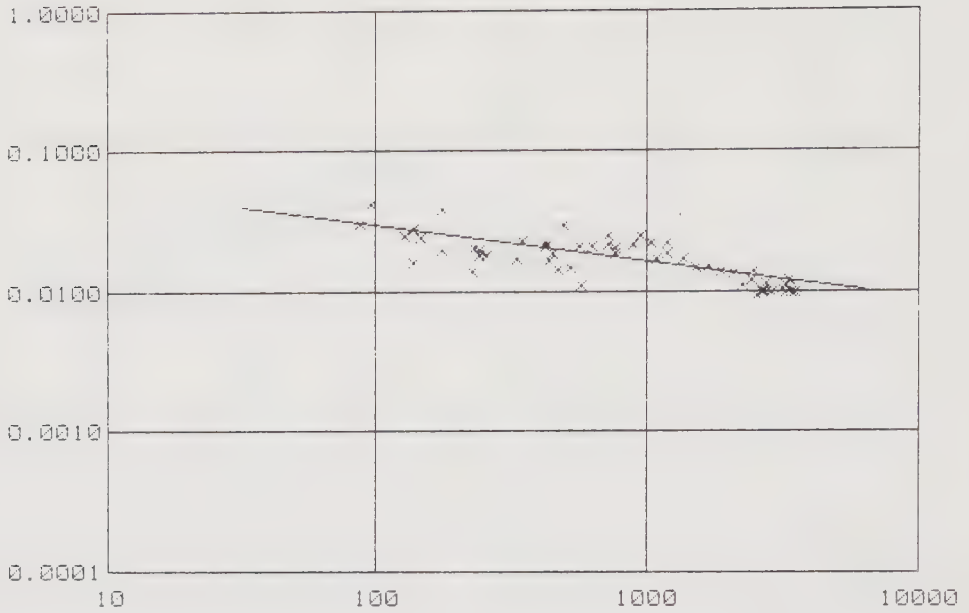
160 mm dense concrete



$$\eta_1 \approx 0.033 (1000/f)^{0.90} \quad f < 1250 \text{ Hz}$$

$$\eta_1 \approx 0.030 \quad f \geq 1250 \text{ Hz}$$

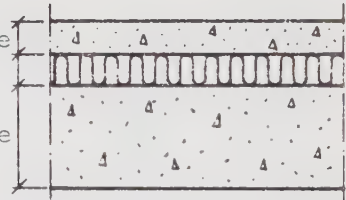
Freq.	η_1
50	0.50
63	0.40
80	0.32
100	0.27
125	0.21
160	0.17
200	0.14
250	0.12
315	0.093
400	0.075
500	0.062
630	0.050
800	0.040
1000	0.033
1250	0.030
1600	0.030
2000	0.030
2500	0.030
3150	0.030
4000	0.030
5000	0.030

Loss factor η 

Frequency (Hz)

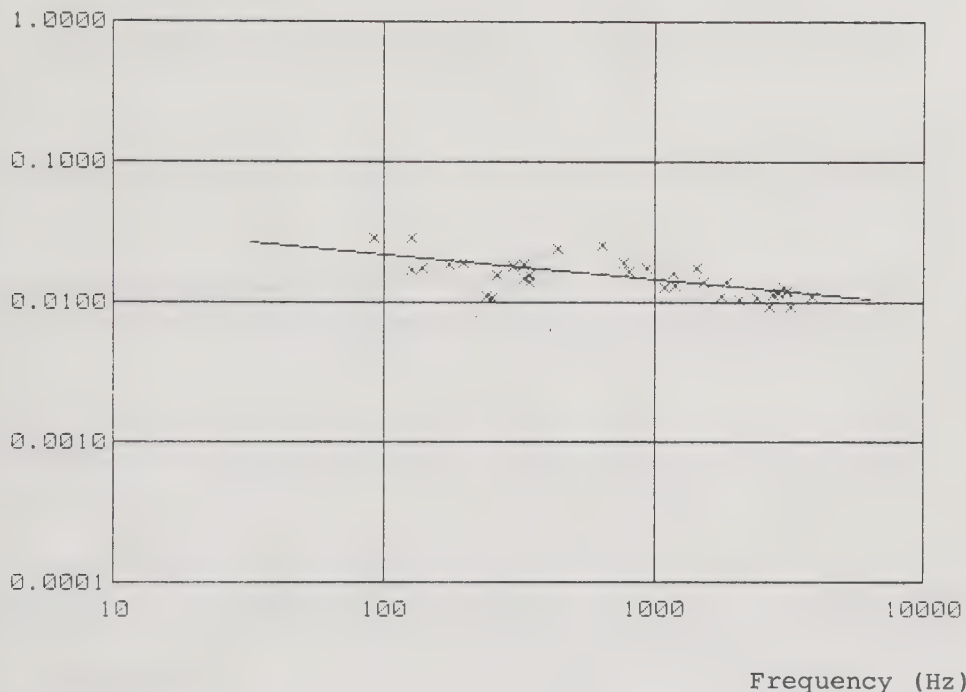
Freq.	η_1
50	0.035
63	0.033
80	0.031
100	0.030
125	0.028
160	0.026
200	0.025
250	0.023
315	0.022
400	0.021
500	0.020
630	0.018
800	0.017
1000	0.016
1250	0.015
1600	0.015
2000	0.014
2500	0.013
3150	0.012
4000	0.011
5000	0.011

50 mm dense concrete
 50 mm mineral wool
 type A
 160 mm dense concrete



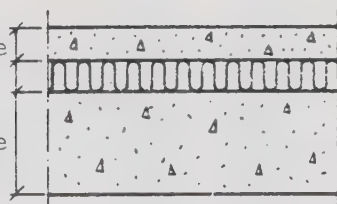
$$\eta_1 \approx 0.016 \cdot (1000/f)^{0.26}$$

Loss factor η

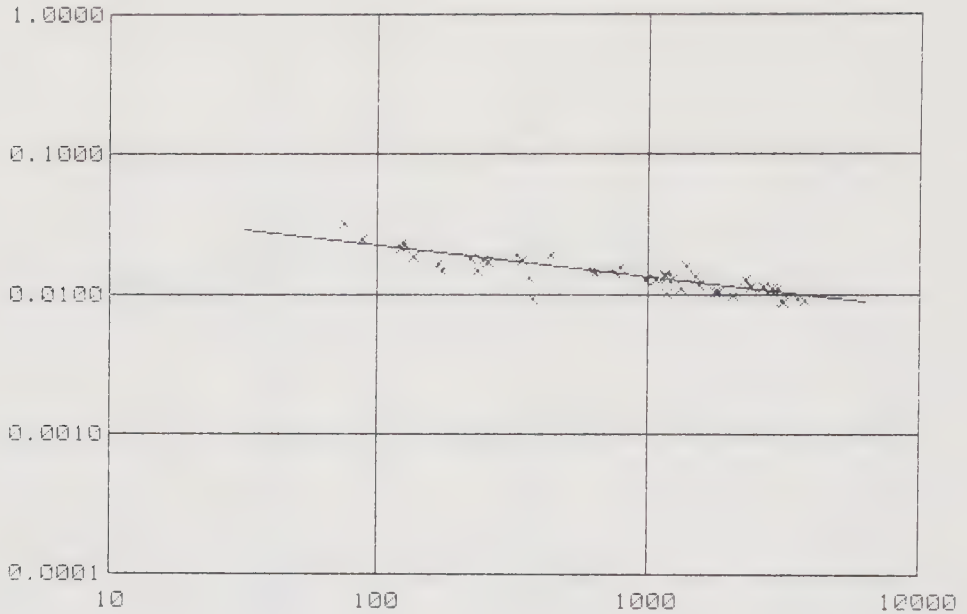


Freq.	η_3
50	0.025
63	0.024
80	0.023
100	0.022
125	0.021
160	0.020
200	0.019
250	0.019
315	0.018
400	0.017
500	0.017
630	0.016
800	0.015
1000	0.015
1250	0.014
1600	0.014
2000	0.013
2500	0.013
3150	0.012
4000	0.012
5000	0.011

50 mm dense concrete
50 mm mineral wool
type B
160 mm dense concrete



$$\eta_1 \approx 0.015 \cdot (1000/f)^{0.17}$$

Loss factor η 

Frequency (Hz)

Freq. η_1

50 0.026

63 0.025

80 0.024

100 0.023

125 0.021

160 0.020

200 0.019

250 0.018

315 0.017

400 0.017

500 0.016

630 0.015

800 0.014

1000 0.013

1250 0.013

1600 0.012

2000 0.012

2500 0.011

3150 0.010

4000 0.010

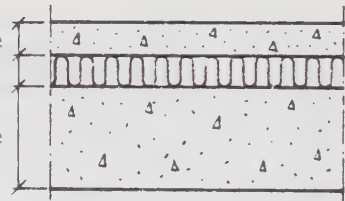
5000 0.009

50 mm dense concrete

50 mm glass fibre

typ C

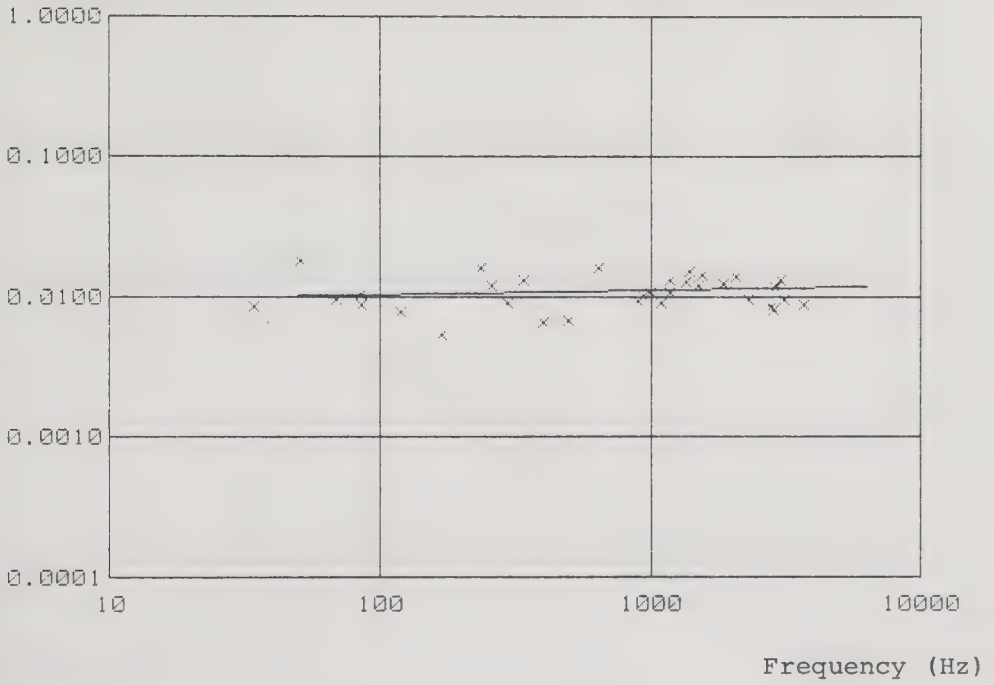
160 mm dense concrete

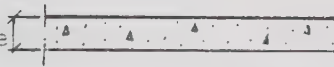


$$\eta_1 \approx 0.013 \cdot (1000/f)^{0.225}$$

V.1.7

Loss factor η

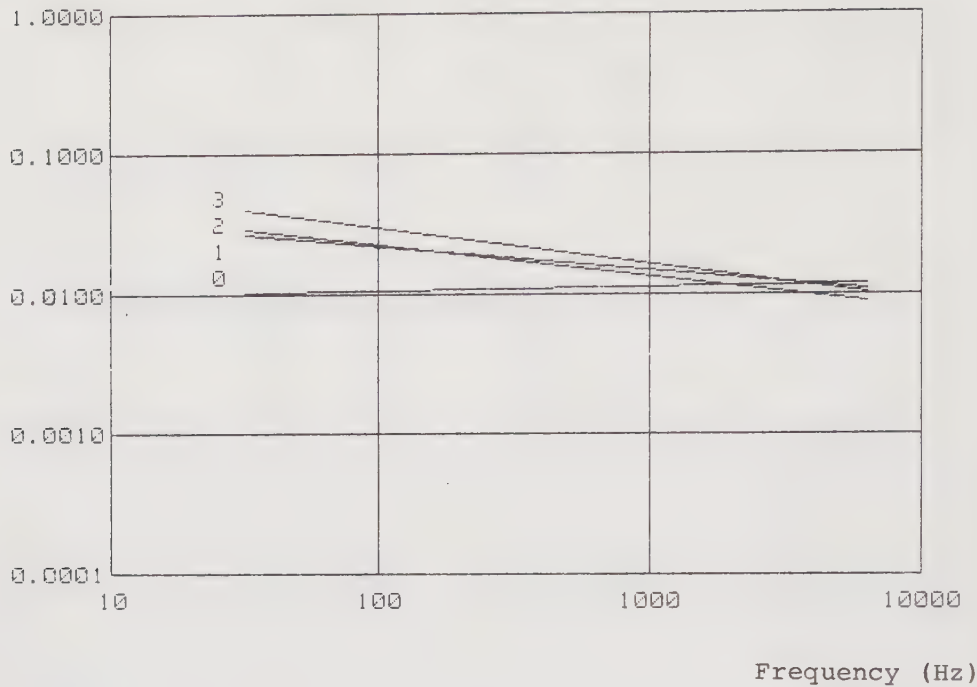


50 mm dense concrete 

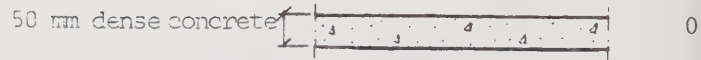
3 x 4 m² Hanging from a crane

$\eta \approx 0.010$

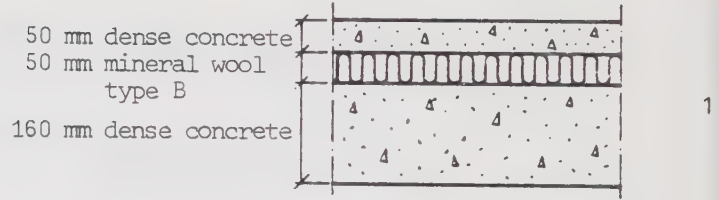
Loss factor η



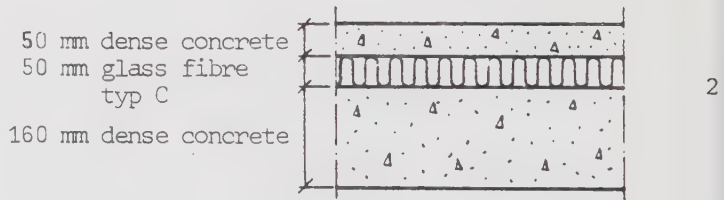
$\eta \approx 0.010$



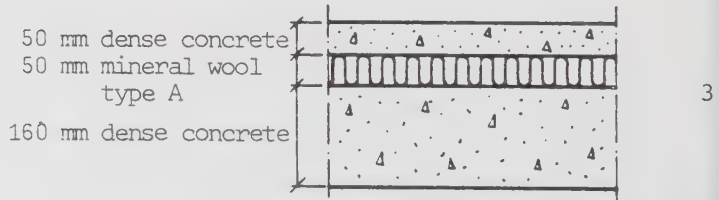
$\eta_1 \approx 0.015 \cdot (1000/f)^{0.17}$

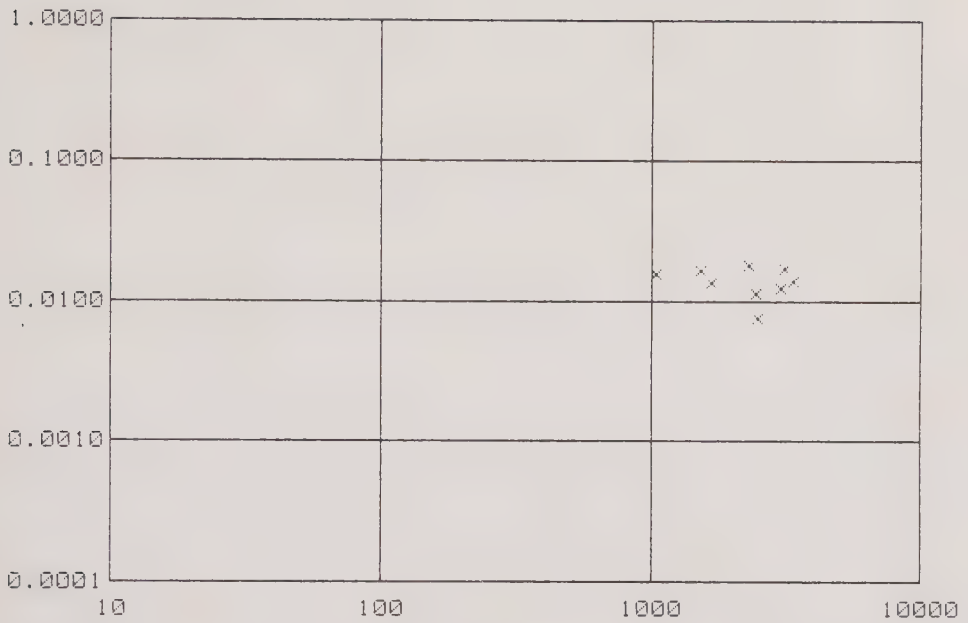


$\eta_1 \approx 0.013 \cdot (1000/f)^{0.225}$

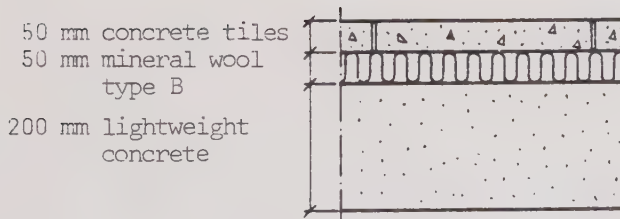


$\eta_1 \approx 0.016 \cdot (1000/f)^{0.26}$



Loss factor η 

Frequency (Hz)



$$\eta_1 \approx 0.014$$

$$f > 1000 \text{ Hz}$$

APPENDIX VI

APPENDIX VI Dynamic modulus and wave velocity of the interlayer

VI.1 Impact sound insulation measurements for different static loads.

- .1 Four different loading conditions for a chip board on mineral wool
- .2 The measurement for 1 kPa static load repeated after a 10 day conditioning period

VI.2 Wave velocity measurements of the different interlayer materials.

	material	measurement distance h (mm)	width of impact device, b (mm)
.1	A	400	25
.2	A	400	25 (0 - 2500 Hz)
.3	A	400	290
.4	B	300	25
.5	B	300	25 (0 - 2500 Hz)
.6	B	300	290
.7	B	300	290 perforated plates
.8	C	200	25 only the
	a	300	25 phase velocity
.9	d	300	25 only the
	g	310	25 phase velocity

VI.3 Wave velocity measurements. The influence of increasing width of the impact device. (The mineral wool used here is approximately the same as "a", but has lower density, only 140 kg/m³).

- .1 b = 10 mm
 b = 25 mm only the phase velocity
 b = 100 mm curves are shown
- .2 b = 180 mm
 b = 290 mm

VI.4 Wave velocity measurements, thick accelerometer plates.

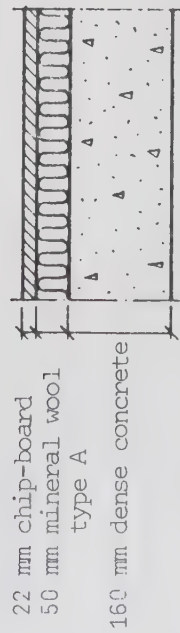
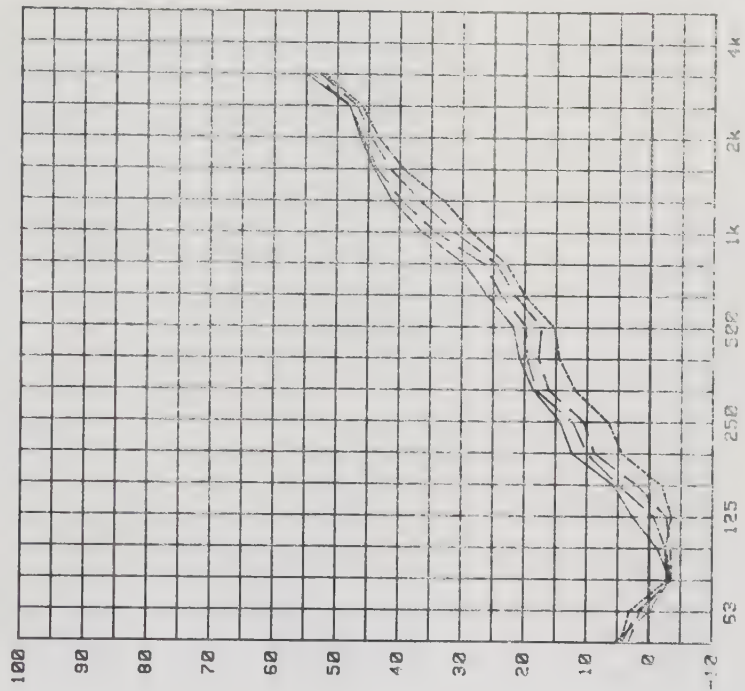
- .1 2 mm aluminium plate

FREQUENCY (Hz)		63	125	250	500	1k	2k	4k												
(1)	----- dXTRA-1	5.4	-0.1	-3.4	-1.3	2.7	5.8	12.4	14.3	18.6	20.8	21.8	26.0	29.6	36.4	41.2	44.1	45.3	49.2	55.0
(2)	----- dXTRA-2	3.2	1.4	-3.4	-2.6	-0.7	4.9	10.0	12.3	19.1	19.6	19.7	23.7	26.5	34.2	39.7	43.6	45.3	49.0	53.9
(3)	----- dXTRA-3	4.2	1.7	-3.6	-3.4	-2.7	2.0	8.8	10.5	16.1	17.7	17.3	21.5	24.5	31.1	36.7	41.5	44.5	46.8	52.6
(4)	----- dXTRA-4	4.5	3.1	-2.8	-2.5	-3.5	-1.9	4.4	6.6	11.9	14.4	15.3	20.0	22.9	28.5	33.0	39.4	43.4	46.1	51.9

Impact sound insulation improvement (ΔL):

- (1) No extra load
- (2) Extra load 150 N/m²
- (3) Extra load 450 N/m²
- (4) Extra load 1 kN/m²

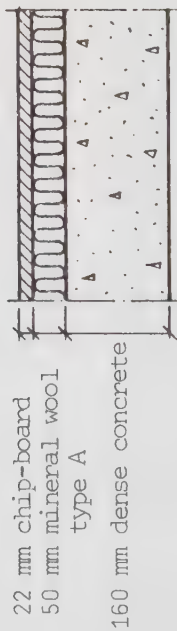
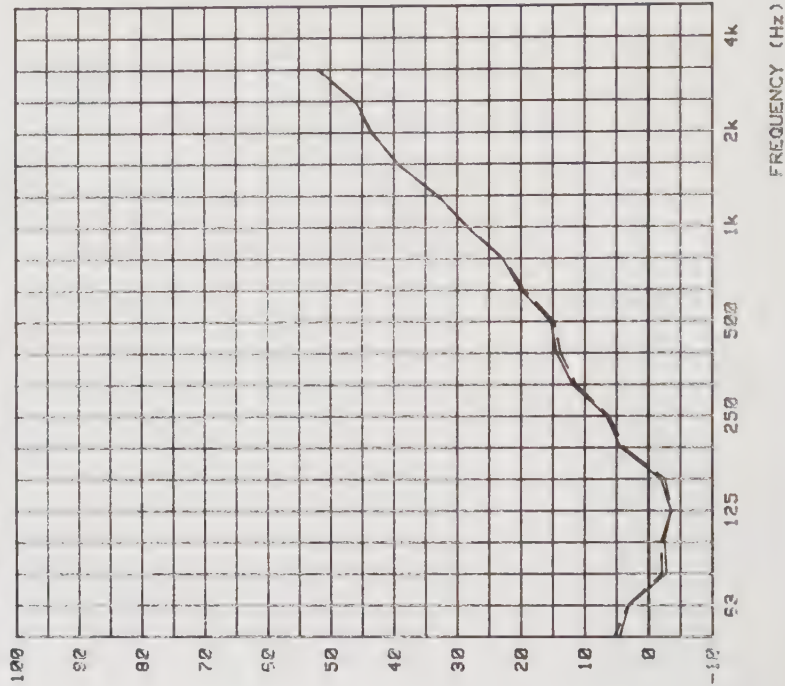
DELTA-L10 (dB)



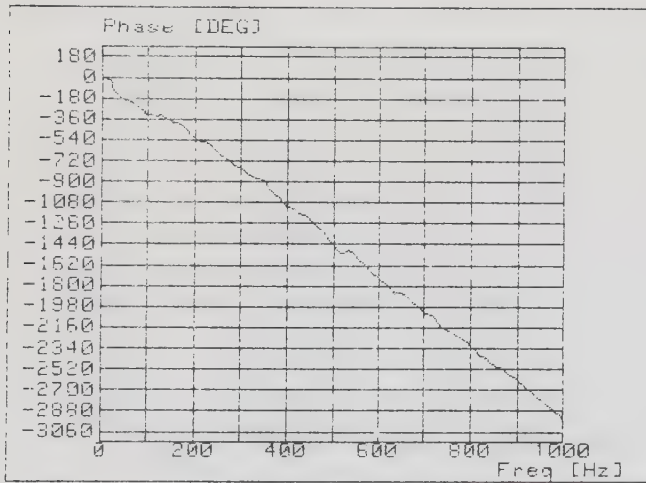
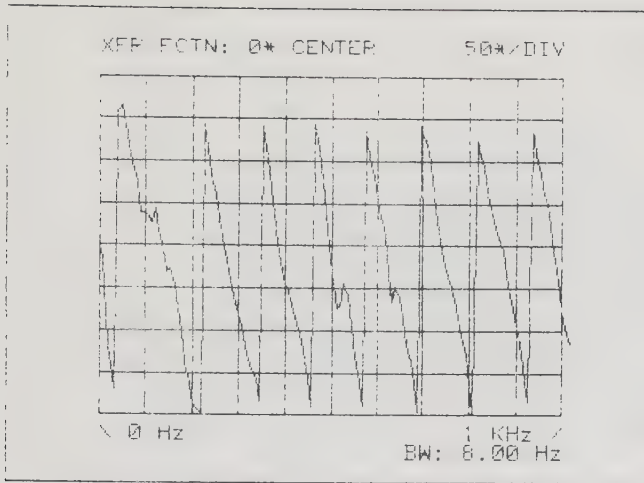
[illegible]

Impact sound insulation improvement (ΔL)
Extra load 1 kN/m²

- (1) Immediately after loading
- (2) 10 days later



VI.2.1

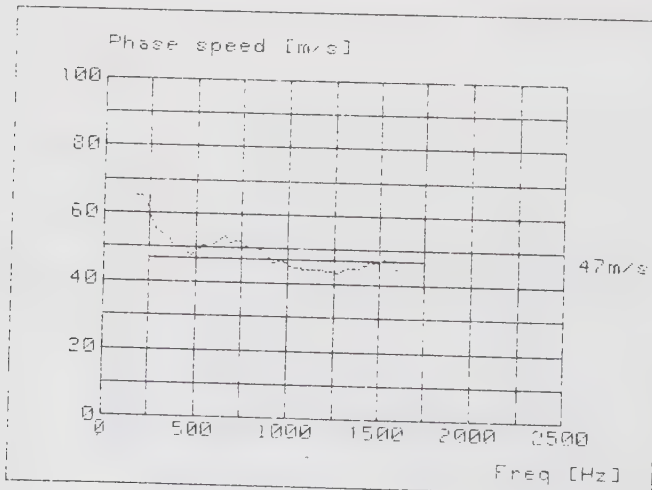
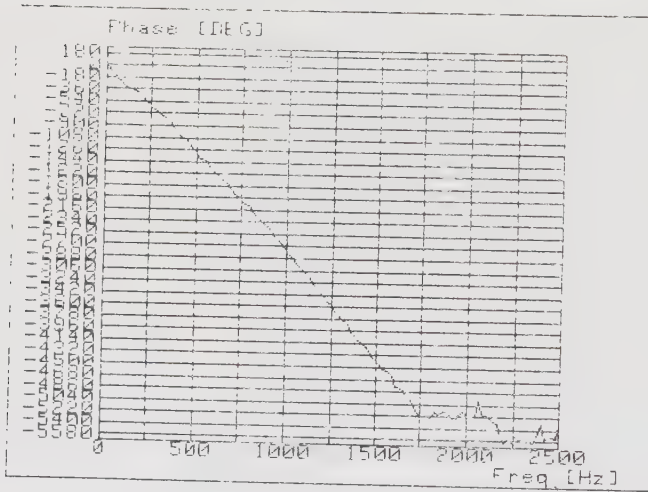
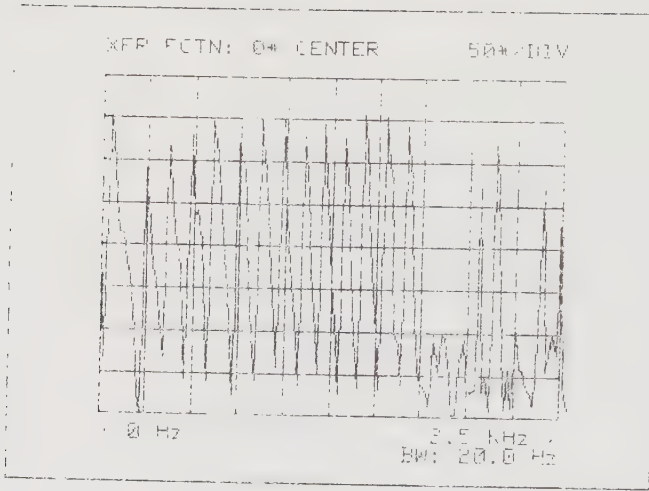


Mineral wool A

$h = 400 \text{ mm}$

$b = 25 \text{ mm}$

VI.2.2

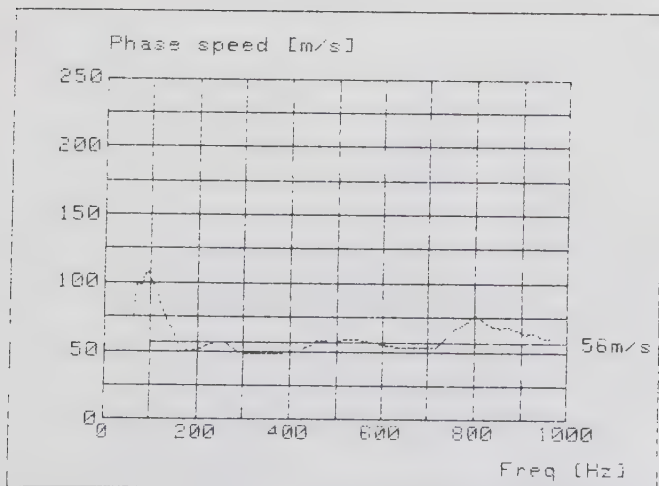
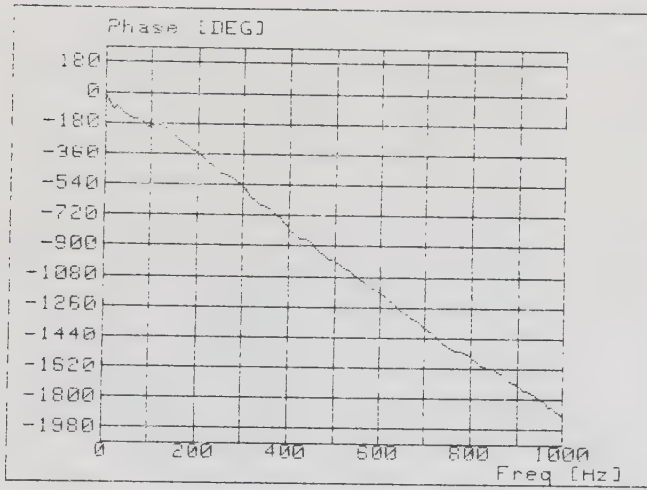
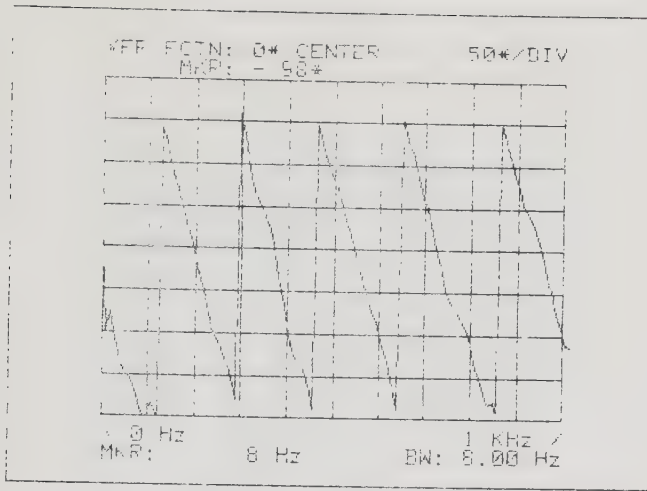


Mineral wool A

$h = 400 \text{ mm}$

$b = 25 \text{ mm}$

VI.2.3

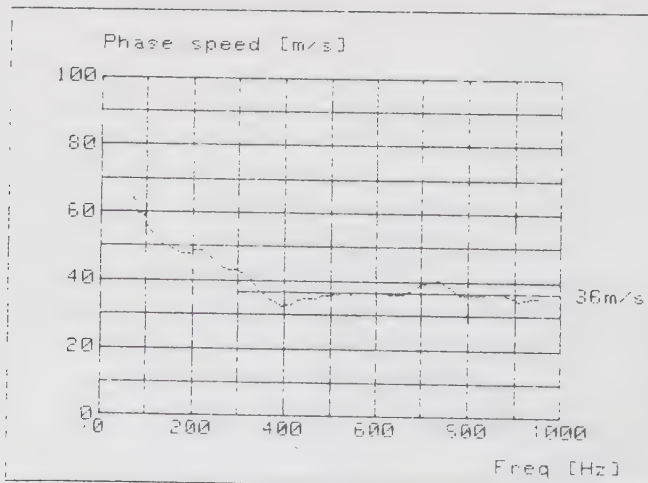
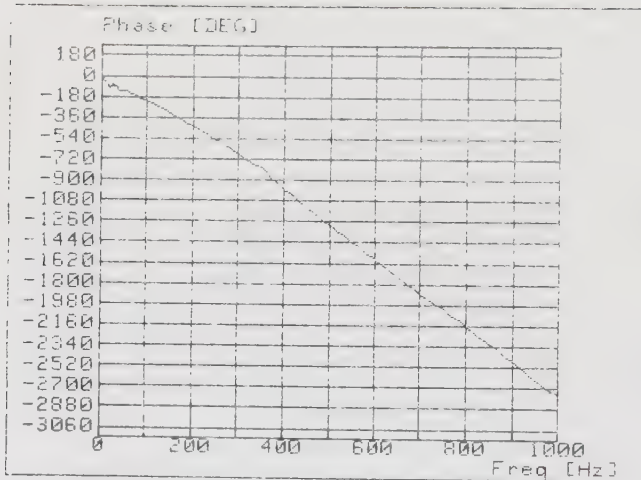
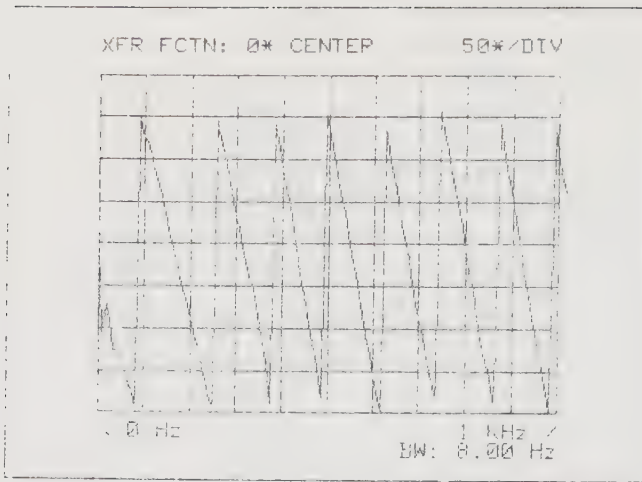


Mineral wool A

$h = 400 \text{ mm}$

$b = 290 \text{ mm}$

VI.2.4

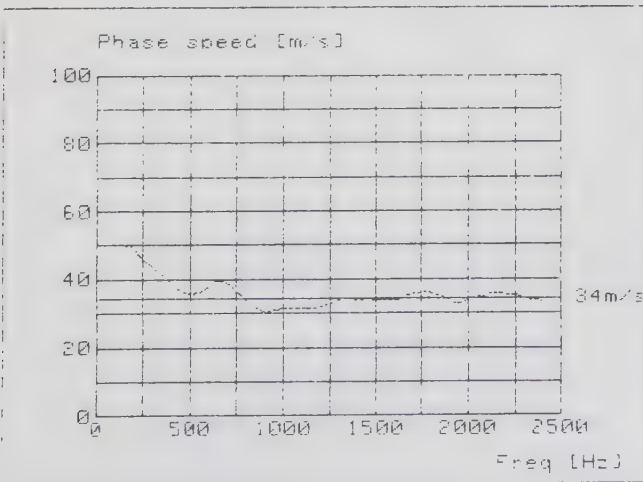
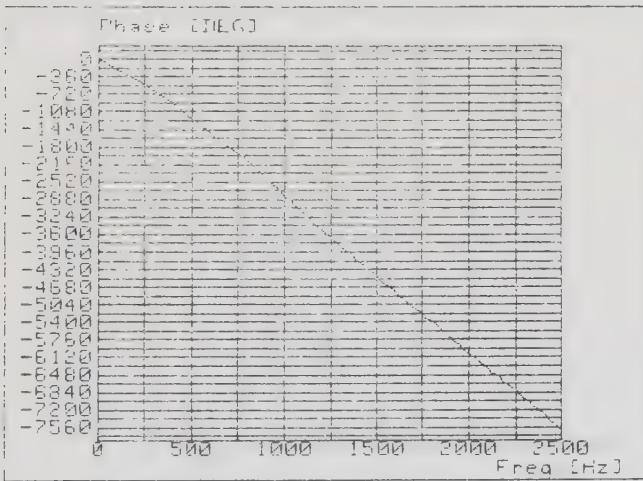
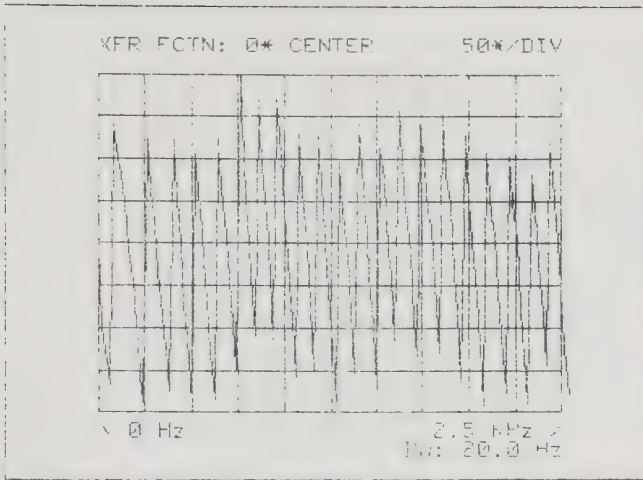


Mineral wool B

$h = 300 \text{ mm}$

$b = 25 \text{ mm}$

VI.2.5

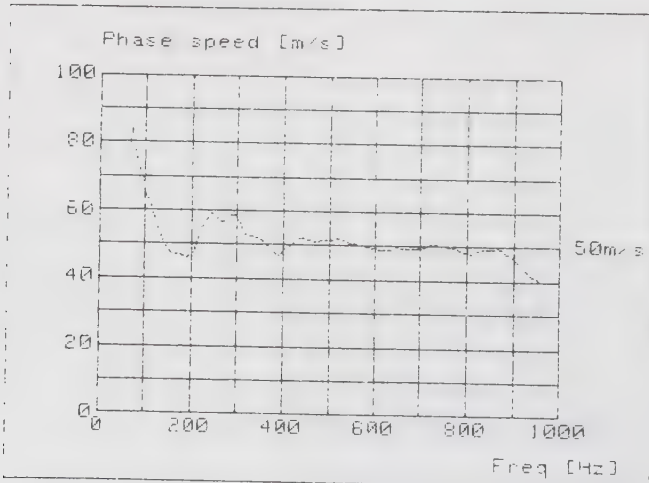
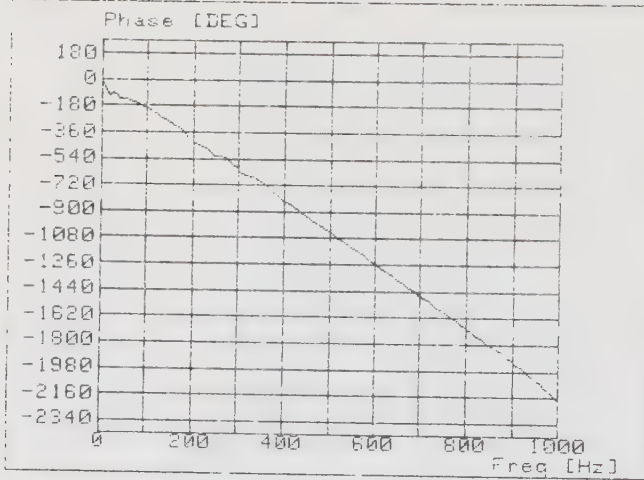
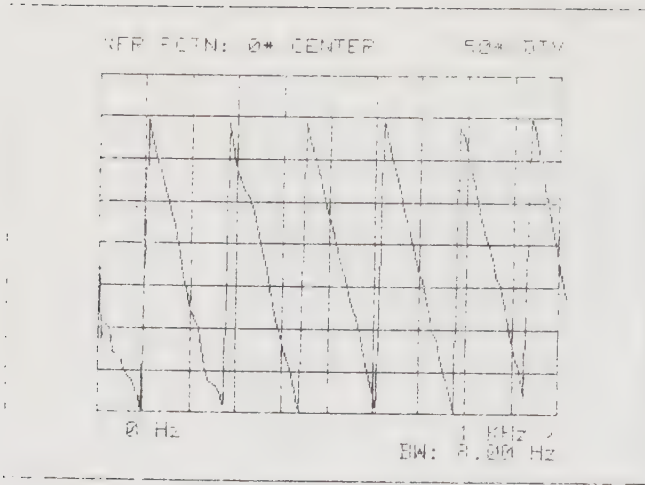


Mineral wool B

$h = 300 \text{ mm}$

$b = 25 \text{ mm}$

VI.2.6

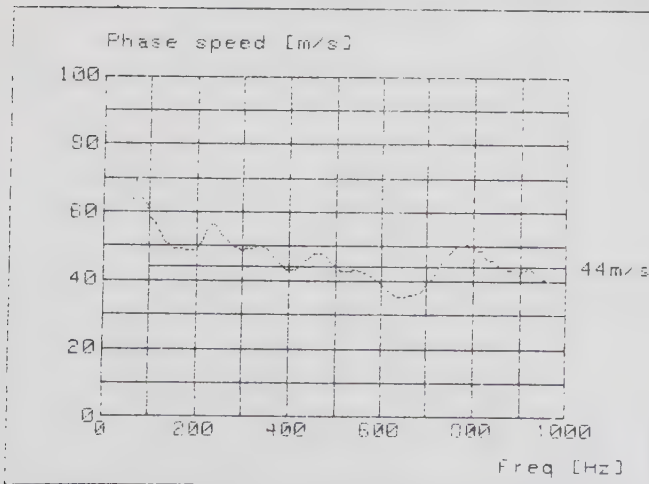
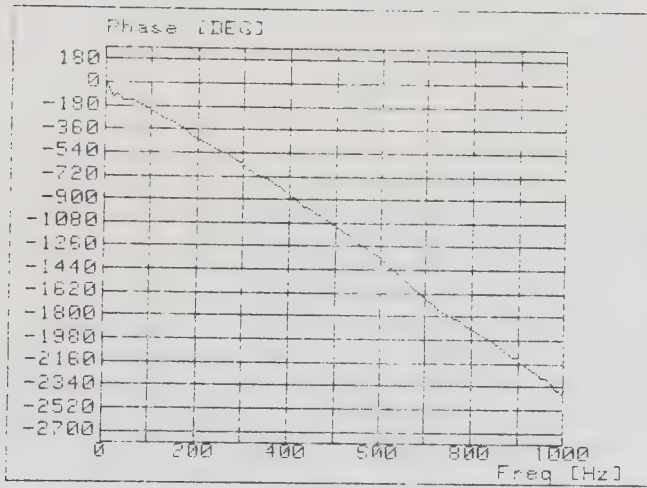
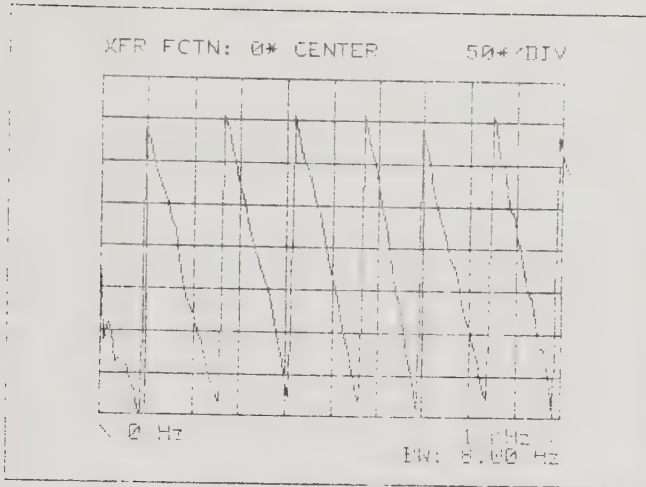


Mineral wool B

$h = 300 \text{ mm}$

$b = 290 \text{ mm}$

VI.2.7



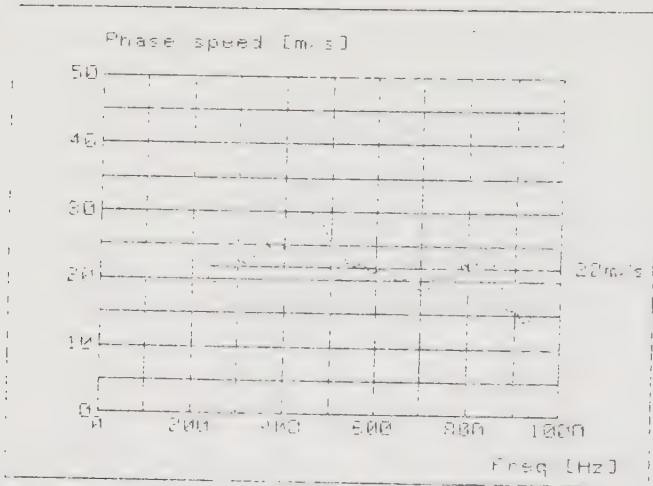
Mineral wool B

$h = 300 \text{ mm}$

$b = 290 \text{ mm}$

perforated
accelerometer
plates

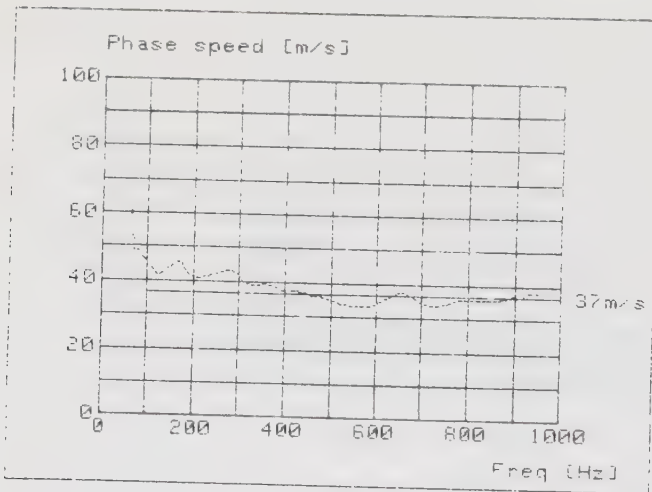
VI.2.8



Mineral wool C

$h = 200 \text{ mm}$

$b = 25 \text{ mm}$

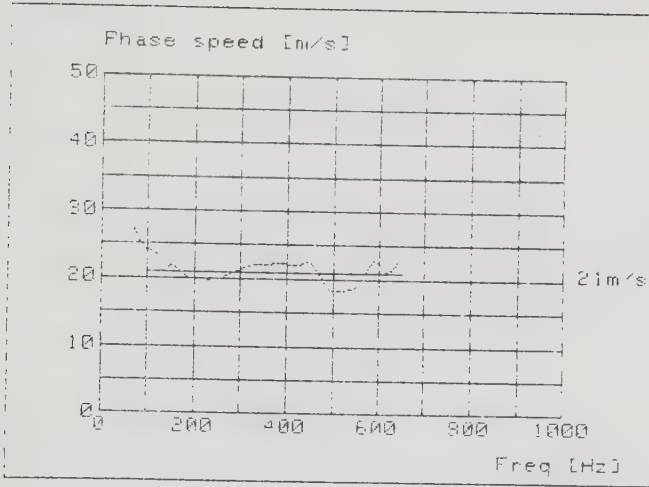


Mineral wool a

$h = 300 \text{ mm}$

$b = 25 \text{ mm}$

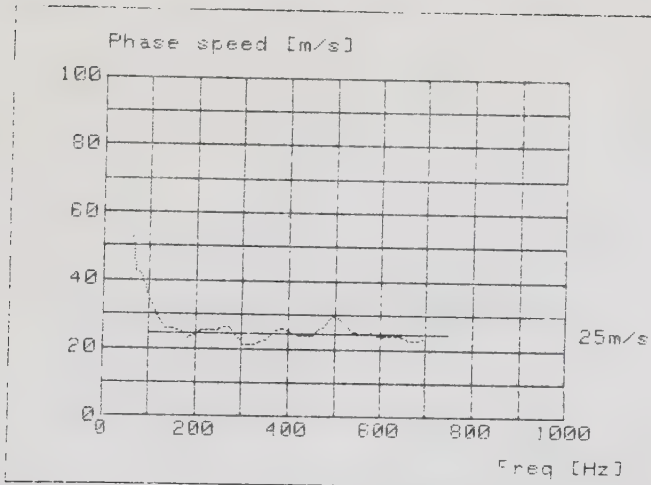
VI.2.9



Foam plastic d

$h = 300 \text{ mm}$

$b = 25 \text{ mm}$

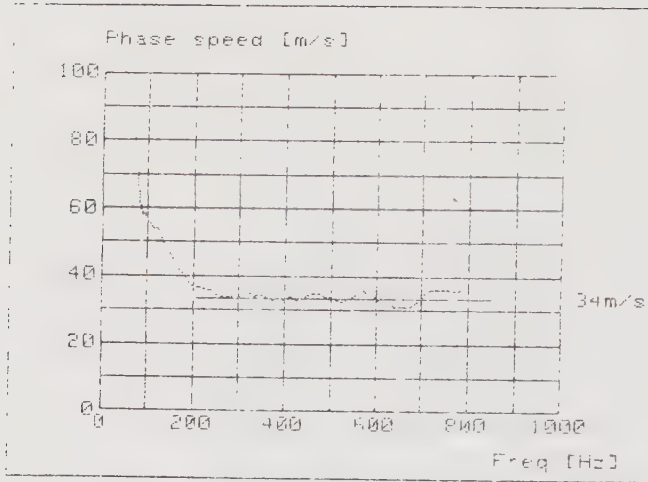


Mineral wool g

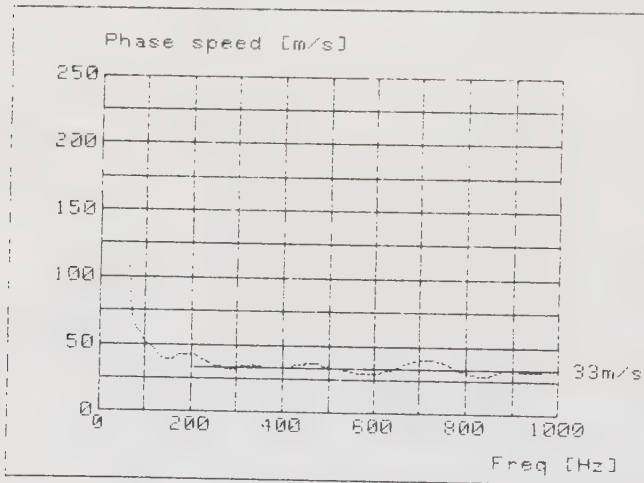
$h = 310 \text{ mm}$

$b = 25 \text{ mm}$

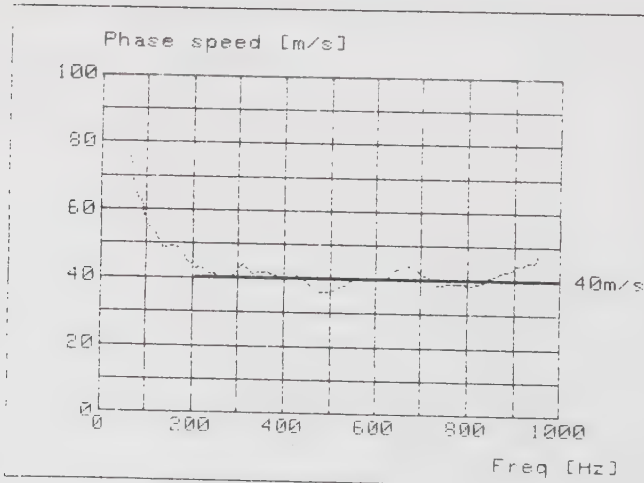
VI.3.1



$b = 10 \text{ mm}$

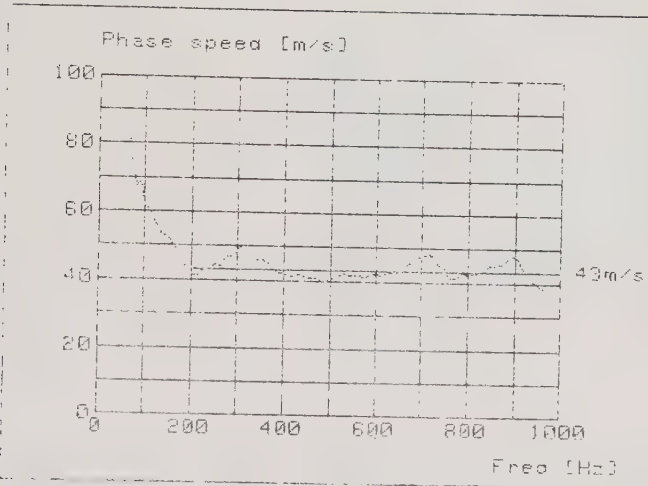


$b = 25 \text{ mm}$

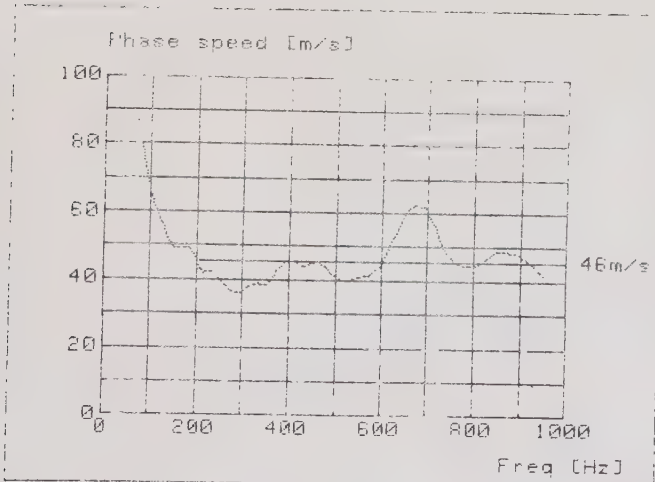


$b = 100 \text{ mm}$

VI.3.2



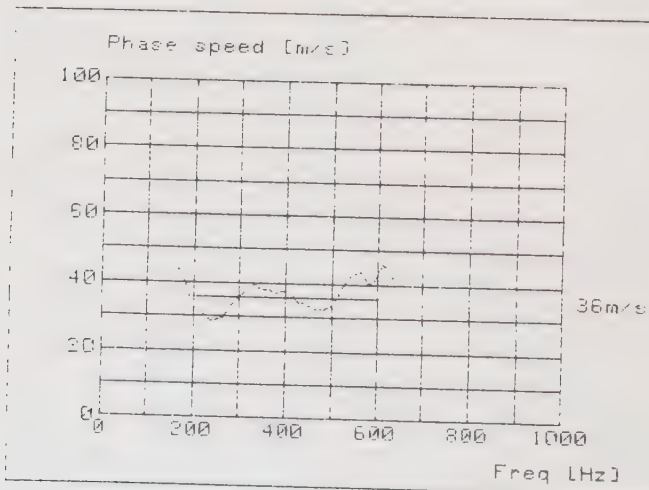
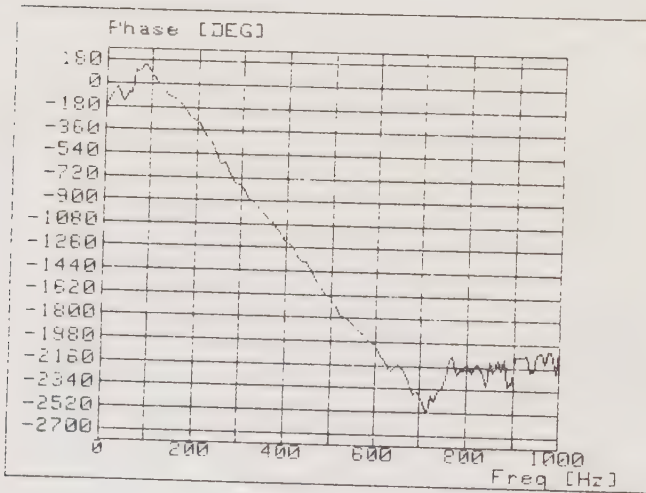
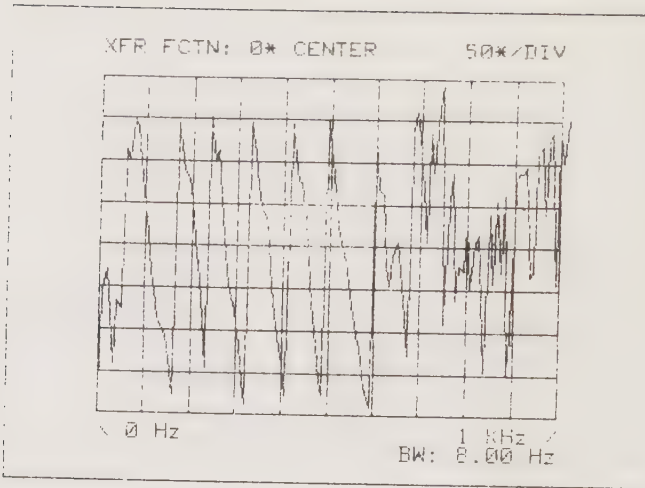
$b = 180 \text{ mm}$



$b = 290 \text{ mm}$

Measurement was also tried for $b = 560 \text{ mm}$ but no valid result was achieved.

VI.4.1



2 mm accelero-
meter plates

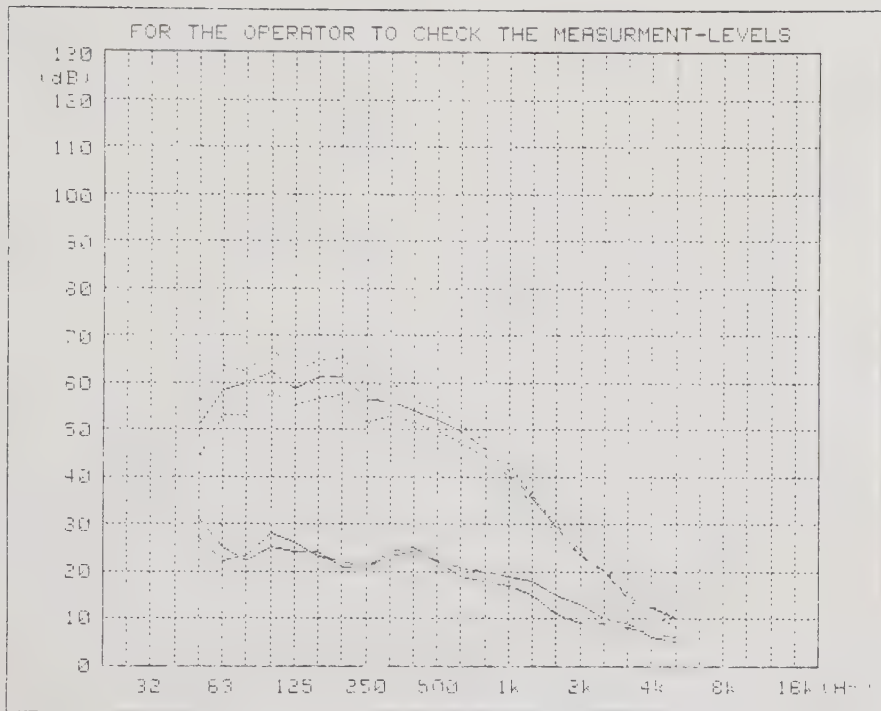
APPENDIX VII

APPENDIX VII

VII.1 Examples of measurements results

- .1 Impact level
- .2 Transmission loss
- .3 Reverberation time

VII.1.1



REPORT 884-

DATE:840116

T60 from File D2:T14

CURVE NO

START-TIME=13:49

L1 = 55 dB

SIGN RM

STOP-TIME=14:25

FREQ	L10	T60
50	50.8	1.97
63	55.6	3.03
80	57.2	3.02
100	59.8	2.8
125	56	2.92
160	59.1	2.65
200	59.2	2.59
250	53.6	2.92
315	53.2	3.11
400	50.8	3.34
500	48.8	3.32
630	46.5	3.22
800	42.8	3.17
1000	38.1	3.2
1250	32.4	3.12
1600	26.7	2.96
2000	21.6	2.53
2500	18.8	2.21
3150	13.8	1.93
4000	12.5	1.55
5000	10.7	1.37

Vol= 99.2

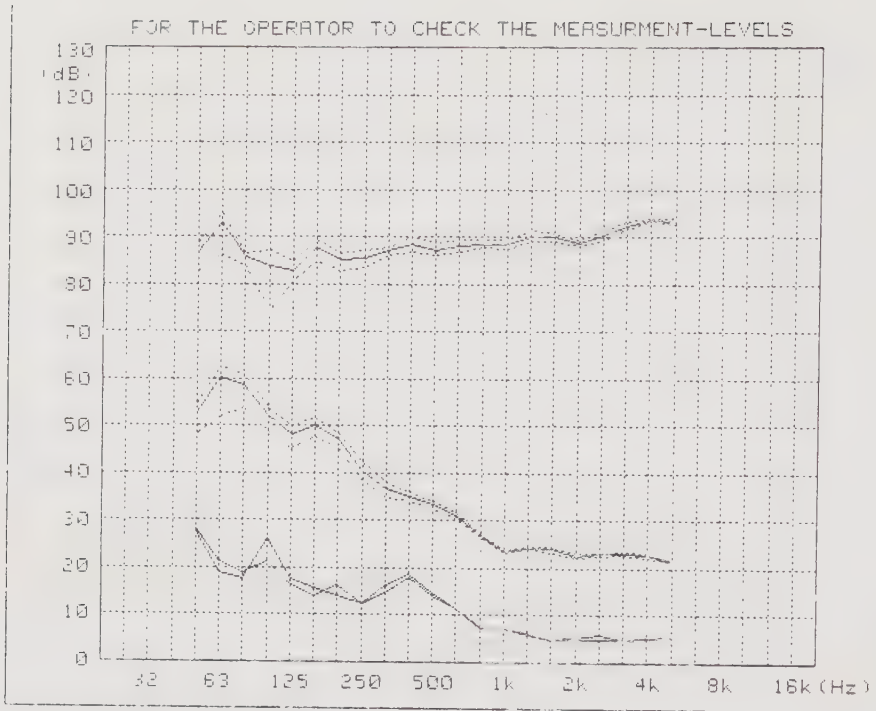
Nmicpos= 3

LIN AVE TIME= 16

MIC FLYTT: LINBANA

L10(f,1) LAGRAS PA :XTa031:T14

VII.1.2



REPORT P84-
DATE:840116
T60 from File D2:T14

CURVE NO
START-TIME=15:38
Rm = 56.9 dB

SIGN
STOP-TIME=15:56
Ia = 59 dB

FREQ	R	Ls-Lm	T60
50	34.7	34.1	1.67
63	36.1	32.3	3.03
80	30.2	27.1	3.02
100	34.9	32	2.8
125	37.7	34.7	2.92
150	40.3	37.7	2.65
200	40.6	38.1	2.59
250	48.2	45.2	2.92
315	53.8	50.5	3.11
400	57.3	53.7	3.34
500	57.4	53.9	3.32
630	60.6	57.1	3.22
800	64.9	61.5	3.17
1000	68.6	65.2	3.2
1250	69.2	65.9	3.12
1600	68.8	65.9	2.86
2000	68.5	66.1	2.53
2500	69.3	67.4	2.21
3150	70.4	69.2	1.93
4000	71.3	71.1	1.55
5000	71.6	71.8	1.37

S= 10.9 Vol= 99.2 Nmicpos= 10
MIC FLYTT: LINEANA P(f),Ia,Rm LAGRAS PA :RTa031:T14

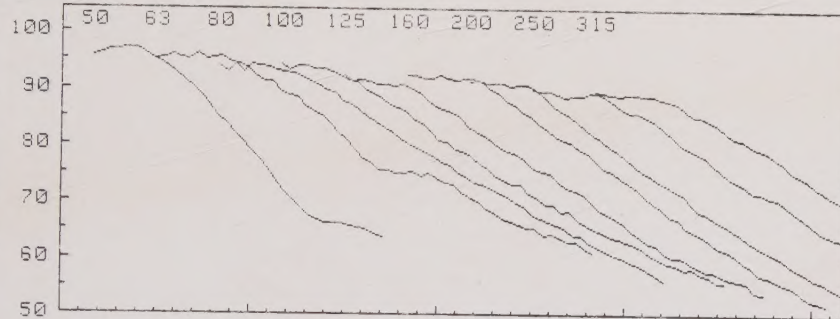
LIN AVE TIME= 16

VII.1.3

DATE:830116 START-TIME~13:24 STOP-TIME~13:39 REC= 5490 / 3206
 MEASUREMENT-RESULT ON FILE D2:T14 ID no: 83:01:16:13:24:03
 DROP= 3 CALC-RANGE= 20 SAMPLETIME= 43.92 Spdk= 130 / 78
 No OF MIC-POS= 10 No OF DECAYCURVES/MIC-POS= 1 Rbnb= 5

FREQ (Hz)	Lin.dev.	REV-TIME (s)	No of Samples
50	.75	1.67	37 / 13
63	31.17	3.03	55 / 23
80	1.92	3.02	56 / 23
100	2.34	2.80	56 / 21
125	1.24	2.92	53 / 22
160	1.30	2.65	53 / 20
200	1.32	2.59	50 / 20
250	3.44	2.92	57 / 22
315	1.32	3.11	60 / 24
400	.57	3.34	56 / 25
500	.99	3.32	59 / 25
630	.66	3.22	57 / 24
800	1.18	3.17	57 / 24
1000	.52	3.20	54 / 24
1250	.32	0.12	55 / 24
1600	.29	2.36	52 / 22
2000	.15	2.53	48 / 19
2500	.05	2.21	45 / 17
3150	.15	1.93	39 / 15
4000	.11	1.55	33 / 12
5000	.15	1.37	29 / 10

***** REVERBERATION MEASUREMENT USING THE INTERRUPTED NOISE METHOD *****



ID no: 83:01:16:13:24:03

TIME SCALE: 100 ms /div

